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AUTOMATIC EQUIPMENT FOR IMPROVING POWER SYSTEM CONTINUITY*

I.A. SYROMIATNIKOV

Moscow

(Received 4 July 1960)

It is a matter of government policy to proceed with the automation and mechanization of production and in this connexion special attention is being paid to low-cost methods of power supply, without the use of circuit breakers on the high-voltage side and the use of short-circuiters and disconnectors. Such methods can only be adopted on the basis of automation.

Widespread use is now made in the U.S.S.R. of such equipment as high-speed protection, automatic excitation regulation of synchronous machines and relay-forcing of excitation, automatic reclosure of lines, bus-bars and transformers, automatic connexion of the standby supply, automatic frequency control, automatic self-synchronization and automatic starting of the hydro-electric units, dispatcher control, and so on. The use of this equipment has considerably improved the continuity of the power supply and made it possible to ensure the static and dynamic stability of the power system.

In this paper it is proposed to review the main automatic equipment which is available in the U.S.S.R. for improving the reliability of power system operation.

Automatic control of excitation. The automatic excitation regulators of synchronous machines are one of the most important means of improving power system reliability. Operating experience and tests which have been carried out in the U.S.S.R. have shown that the main requirement of automatic excitation regulators is to force the excitation voltage up to its maximum value.

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* *Elektrichestvo*, 7, 18-24, 1961.

A system of compounding with an electromagnetic corrector is now extensively used for automatic excitation of regulation. It is supplemented by relay forcing of excitation which ensures maximum excitation if the voltage drops by more than 15 per cent. A new phase compounding system has been developed in which the current in the excitation winding of the exciter depends on the phase of the stator current as well as its magnitude. This includes the control of voltage under normal conditions and reduces the power of the corrector.

In order to improve static and dynamic stability in long distance transmission, use is also made of high response regulators which react to the voltage, current, and the first and second derivatives of the current and ensure the desired level of voltage on the high-voltage bus-bars after the transformers under normal conditions.

The exciters of generators which are used on long distance transmission must have higher limits and rates of voltage build-up. Thus, the ceiling of the voltage of the excitors for the generators at the Volga hydro-electric power station and the Stalingrad hydro-electric power station is four times their rated voltage, whilst the rate of voltage build-up is five times greater. At such high values, ionic excitation is much more economical. However, tests at the Volga hydro-electric station have shown that the maximum power under conditions of static stability is approximately the same for machine excitation with a maximum voltage 1.8 times greater than the rated voltage if a high response regulator is used, and the time constant of the excitation winding is 0.2-0.3 sec as for ionic excitation with a ceiling four times greater than the rated voltage. The results were only slightly less satisfactory in relation to dynamic stability.

The use of high response automatic excitation regulators is not the complete answer to the problem of ensuring dynamic stability, but this problem can be solved by such means as high-speed clearance of short-circuits, mechanical or electrical braking of generators and the disconnexion of part of the generators.

Automatic frequency control. Serious faults due to a deficiency of active power can be reduced by using automatic devices which disconnect the consumers if the frequency drops to a dangerous level.

It is considered dangerous if the frequency drops to 47-48 c/s for a long period of time. Such a drop in frequency reduces the efficiency of fans, flue gas pumps and especially the feed pumps of fuel-burning power stations. The output of the station is reduced as a result and the deficiency of power in the system is aggravated so that frequency can be reduced in a vicious spiral Automatic load shedding devices begin to act at the frequency of 48 c/s.

In selecting which loads are to be shed, it is necessary to consider the maximum generator power which can be disconnected in the power system in question. Consumers must only be disconnected one group at a time in order to avoid unnecessary inconvenience. The frequency must be raised to 49-50 c/s after the load shedding devices have operated.

After the main groups of consumers have been disconnected, the frequency can still settle at 48 c/s. A special device is provided which cuts off another group of consumers at 47 c/s with a time lag of 20-30 sec.

The efficiency of this load-shedding system depends upon the instantaneous operation of the automatic equipment. It is, however, necessary to consider the possibility of the frequency relays operating incorrectly owing to sudden changes in voltage during short-circuits. The relay contacts may be momentarily closed by transient conditions in the relay winding circuits. A minimum time delay of 0.5 sec is therefore allowed.

Consumers are automatically reconnected by the automatic reclosure system when the frequency is restored.

The average number of successful operations of such frequency regulators is shown in Table 1.

Over the past eleven years each device operates about 1.9 times per year.

The maintenance of normal voltage when a reduction in frequency occurs results in an increased consumption of reactive power at a time when the supply is restricted. It is therefore desirable to reduce the voltage if the frequency is reduced.

Soviet power systems which are served by hydro-electric power stations have a system of automatically starting and connecting standby hydro-electric generators when the frequency is reduced. Total load is reached 60-90 sec after starting. Use is made of speed governors to load partially unloaded hydro-electric generators and to convert hydro-electric generators operating as synchronous condensers. As a result partially unloaded generators take up their full load after 3-5 sec, and those operating as condensers after about 30 sec.

The device which is used for automatically starting and connecting

TABLE 1

Years*	1945	1946	1947	1948	1949	1950	1951	1952	1953	1954	1955	1956	1957	1958	1959
No. of frequency regulators	96	130	157	265	398	594	631	711	730	827	1 011	1 083	—	1 349	1 526
Percentage of 1945	100	136	163	275	415	620	660	740	780	863	1 050	1 240	—	1 400	1 600
No. of successful operations	—	—	—	325	351	355	472	495	462	353	489	319	—	405	703
Average number of operations	—	—	—	0.81	1.12	1.2	1.34	1.44	1 58	2.34	2.1	3.4	—	3.33	2.17

* As at 31 December each year

hydro-electric generators incorporates a frequency difference relay (for the difference between the generator and network frequency), which operates in the presence of about 5 per cent slip and connects the generator in the network without synchronization. If several generators have to be started, the starting signals are sent off at intervals of about 20-30 sec.

Automatic connexion of standby supply. This type of automatic equipment was first successfully used for power station auxiliaries. It was extended to the network and installed in industrial enterprises later. This system is used to connect standby transformers, lines, motors and sectional circuit breakers whenever the voltages disappear, including short-circuits on the bus-bars. Operating experience shows that the supply of a voltage after a short-circuit on the bus-bars has in many cases been successful in preventing a fault.

Table 2 shows the average number of successful operations of standby supply controllers.

The controllers are usually operated by the auxiliary contacts of the main circuit breaker or a voltage relay which reacts to the disappearance of voltage on the bus-bars from which the consumers are supplied. In the first case, the standby supply is connected instantaneously on disconnexion of the supply source, but in the second case the voltage relay operates even if the voltage only disappears momentarily and so a time lag is provided.

During the past twelve years each controller has on average operated 4.4 times per year.

Automatic reclosures. Automatic reclosure is extensively used on overhead lines and cable runs, substation bus-bars, transformers, motors, generators and condensers.

The statistics of automatic reclosure devices are given in Table 3. On average each device has operated 1.34 times per year over the past fifteen years.

The use of automatic reclosure on overhead lines is obligatory. Three phase instantaneous automatic reclosure is mainly used, but single-phase automatic reclosure is used on important transit and spur 110-220 kV lines and in networks with large earth faults. Single-phase automatic reclosure makes it possible to use lines on two phases without interrupting the supply. Unfortunately, single-phase automatic reclosure is a relatively complicated system.

TABLE 2

Years*	1945	1946	1947	1948	1949	1950	1951	1952	1953	1954	1955	1956	1957	1958	1959
No. of standby supply controllers	274	400	483	633	941	1 143	1 662	2 204	2 792	3 567	6 142	7 370	8 852	10 592	11 092
Percentage of 1945	100	146	176	231	308	418	610	805	925	1 300	2 240	2 700	3 230	3 900	4 370
No. of successful operations	—	—	—	120	196	171	225	611	640	1 049	1 404	1 479	1 498	2 686	3 082
Percentage	—	—	—	75.5	90.8	95	81	95.3	95.9	96.5	94.3	96.2	95.3	96	96.5
Average number of operations	—	—	—	5.27	4.8	6.7	7.4	3.61	4.36	3.4	4.3	5.0	5.9	4	3.9

* As at 31 December each year

Operating experience has shown that automatic reclosure can be successfully used on cable runs with intermediate substations. Thus, for example, 54.5 per cent of all automatic reclosings on cable runs were successful, which compared favourably with 59.2 per cent on overhead lines. This is obviously explained by the fact that a large number of short-circuits occur on the bus-bars of substations.

Automatic reclosure can be successfully used for the supply of voltage to substation bus-bars no matter what the reason for the disappearance of the voltage. In this case automatic reclosure is usually operated by the differential protection of the bus-bars. However, if there is one transformer at a stepdown substation it is necessary to use automatic reclosure which is operated from the current protection. The automatic reclosure of transformers is operated by the auxiliary switches of circuit breakers, but they are interlocked with the internal protection of the transformer.

Statistics show that 75-80 per cent of all bus-bar and transformer automatic reclosings are successful. Each successful operation prevents the disruption of the power supply for a large number of consumers.

It is well known that the self-starting of all motors is not always successful after the clearance of short-circuits. Use may therefore be found of alternative automatic reclosure for maintaining important synchronous and asynchronous motors in operation. Experience with this type of automatic reclosure in the oil fields in Baku for the pump motors has shown that it is extremely efficient and reliable. This type of automatic equipment could be used at power stations and in other plants where there is no maintenance staff on duty to start the motors which were disconnected in order to facilitate the self-starting of the the important motors.

Automatic reclosure after restoration of frequency was first used in the White Russian power system on the suggestion of I.N. Aleksandrov and A.Sh. Fridliand. Now they are in use in a number of power systems. This type of automatic reclosure must be considered very promising in connexion with the introduction of automatic equipment for starting standby units in the event of a reduction in frequency.

Use is now made in some cases of boiler-turbine-generator-transformer-line units with about 30 breakers in the electric circuit. This involves the widespread use of automatic reclosure devices in conjunction with self-synchronization devices.

In the event of a line short-circuit, the unit is disconnected at the

TABLE 3

Years*	1945	1946	1947	1948	1949	1950	1951	1952	1953	1954	1955	1956	1957	1958	1959
No. of automatic reclosure devices	1 074	1 229	1 518	1 945	2 416	2 875	3 486	4 127	4 736	5 636	7 129	8 787	10 198	12 630	15 272
Percentage of 1945	100	114	141	181	225	266	325	380	440	523	663	820	950	1 180	1 420
No. of successful operations ..	744	1 063	908	1 416	1 574	1 987	2 104	2 843	3 585	4 560	5 330	6 682	8 019	9 878	12 111
Percentage	61	60	56.6	67.2	69.3	69.7	70.8	68.4	68.1	70.6	71.4	69.7	69	68	69
Average number of successful operations of one device	1.5	1.2	1.69	1.37	1.53	1.44	1.63	1.45	1.32	1.24	1.33	1.32	1.27	1.28	1.26

* As at 31st December each year

receiving substation and the device for extinguishing the generator field is disconnected from the line protection. The residual voltage of the generator is so small after extinction of the field that it is in no state to maintain the arc at the point of short-circuit. After the required period of time for restoring the electrical strength of the insulation at the point where the fault has occurred, automatic reclosure takes place at the receiving substation and the device for extinguishing the field of the generator is connected. In order to reduce the extinction time on the field and ensure a more rapid connexion of the generator into the network, it is necessary to use an AGP-1 type field extinction device with de-ionic grid. In order to avoid dangerous overvoltages on the excitation winding of the generator, it is necessary to provide a resistance in the field extinguishing device which shunts the excitation winding.

The use of high-speed governors for the turbines makes rapid synchronization of the generators possible.

Non-synchronous automatic reclosure is also very promising. Tests and operating experience in the U.S.S.R. show that in some cases it can even be successful from the point of view of preserving the equipment.

Non-synchronous reclosure is however impracticable if the moments arising with it are greater than the moments which arise during three-phase short-circuits for which all generators are designed. It has been established that such electromagnetic moments occur in turbine generators at currents exceeding 5 I_{nom}, and at current exceeding 3.5 I_{nom} in hydro-electric generators.

Swings which occur during non-synchronous reclosings can in some cases cause the incorrect operation of the relay protection and additional measures must be taken to prevent this.

Non-synchronous automatic reclosure devices are very simple in design. They have therefore been extensively used in Soviet power systems. They are particularly effective on long inter-system links.

The extensive use of non-synchronous automatic reclosure has been instrumental in the large scale use of excitation forcing for generators so that the operation of the power system is unaffected by sudden changes in the current and voltage or the swings which appear during non-synchronous reclosing.

High-speed reclosure also deserves special attention. This equipment provides a "currentless pause" for the air blast circuit breakers equal to 0.2-0.3 sec. In many cases the synchronous operation of the generators is not disturbed during the operating time of such devices and reclosure is not accompanied by out of step conditions. This feature is very important for the reclosure of large power stations. High-speed automatic reclosure may however produce undesirable effects.

In particular, high-speed reclosure of heavily loaded lines has no real advantage over non-synchronous reclosure since the rotational speed of the generators is rapidly increased if the total load is suddenly shed and the angle δ between the voltage vectors at the ends of the line increase sharply during the currentless pause.

The use of high-speed automatic reclosure is possible provided that use is made of high-speed protection which will provide simultaneous and instantaneous disconnexion of the line at both ends independently of the position of the faults.

On single lines which link a power station with a system or two parts of a system, use may also be made of three-phase automatic reclosure with recovery of synchronism besides single-phase and non-synchronous automatic reclosure.

It is necessary to consider out-of-step conditions in somewhat more detail.

Until automatic excitation regulation was used on a large scale, cases occurred when, despite the measures which had been taken, automatic reclosure was not followed by synchronism; out of step conditions set in, and the operation of the power system was disturbed. This was the main reason for failing to use non-synchronous automatic reclosure. However, it was seen later that sustained non-synchronous conditions were in the majority of cases permissible in power systems where the synchronous machines were provided with automatic excitation regulation, since the stability of the power system was not disturbed. The use of non-synchronous automatic reclosure was limited in these cases only by the mechanical forces arising in the generators, transformers and other equipment. However, out-of-step conditions must not be allowed to continue indefinitely since this may disturb the normal operation of consumers who are attached to substations in the electrical centre of the swings.

In all cases when sustained out-of-step conditions are probable and the use of non-synchronous automatic reclosure is permissible, it is necessary to take measures to ensure re-synchronization and prevent sustained out-of-step conditions. These measures include the installation of quick-acting speed governors on the steam turbines, the mechanical or electrical braking of hydro-electric generators, the disconnexion of some of the generators, load shedding and other measures.

Nevertheless operating experience shows that the use of non-synchronous automatic reclosure should not be restricted owing to the danger of sustained out-of-step conditions. Thus, from 1954 to 1958, non-synchronous automatic reclosure devices operated a total of 339 times, of this 257 were successful. Only three unsuccessful operations were attributable to sustained out-of-step conditions.

Two of these cases occurred in 1956 on the line linking the Azerbaijan and Gruzinsk power systems under abnormal conditions. The link between these systems was quite loose, and its stability was often disturbed with sudden changes in the load of the consumers. During the two years 1955 and 1956 there were 46 cases of non-synchronous automatic reclosure, of which 32 were successful. Twelve of these cases were caused by stable short-circuits on the line and only two cases were attributable to sustained out-of-step conditions.

The third unsuccessful operation due to sustained out-of-step con ditions occurred in the Permsk power system which was connected with the Kamsk hydro-electric station by two 220 and 110 kV lines. If large outputs were transmitted along the 220 kV line before disconnecting it, the stability of transmission along the 110 kV line was disturbed. But even here there were several dozen cases of successful operation of non-synchronous automatic reclosure on 220 kV lines and only in one very difficult case was it necessary for the staff to interfere and eliminate the out-of-step conditions. After this use was made of automatic disconnexion of some of the generators of the Kamsk hydro-electric station on interruption of the 220 kV line and no further cases of unsuccessful operation of the non-synchronous reclosure occurred.

Aspects of static and dynamic stability. In individual power systems fault disconnexions of consumers have occurred owing to "voltage avalanches" at a nodal centre with a small drop in voltage in the supply network.

The typical feature of centres where this has happened is the preponderance of induction motors for which the reactive power requirements are mainly covered by static capacitors. The average load of induction motors in such conditions is 0.8-0.9 of nominal.

A radical way of preventing the consumer being cut off under fault conditions is to use synchronous motors instead of asynchronous motors or to use synchronous condensers instead of static capacitors, and to force the static capacitors at reduced voltage. In this case the starting devices may react to the rate of change in the voltage with time.

In some cases it is not possible to use static capacitors to compensate the reactive power on the grounds of stability. However, the use of synchronous motors instead of asynchronous motors with static capacitors is more economical in the majority of cases.

Large turbo-generators with direct cooling have a relatively large reactance and small mechanical time constants. This is explained by the fact that new systems of cooling make it possible to increase the output considerably at the same overall dimensions. The synchronous torque of such generators is less and their resynchronization is more difficult in consequence. Owing to their intense cooling they have considerably greater current densities under normal conditions and a lower overload capacity. The margins of mechanical strength are also smaller.

The problem of the stability of turbo-generators can be solved by using speed governors on steam turbines which react more quickly to acceleration. Tests on one such regulator which was designed by the All-Union Thermal Technical Institute proved satisfactory. Thus, for example, synchronous operation of the generator was ensured with the disconnexion of one parallel line by reducing its output. Without the regulator the generator fall out of step. The regulator was also instrumental in re-synchronizing the generator after short-circuits lasting 0.5 sec. The synchronism was not restored after such a short-circuit without the regulator.

The Leningrad Metal Factory has developed a hydraulic differentiator which reduces or stops the supply of steam to the turbine roughly 0.2 sec after a sudden drop in the load.

Owing to a number of specific features, it is not possible to make the speed control of a water turbine more quick-acting. The problem of improving the stability of hydro-electric generators has to be solved by other means, such as electrical braking, disconnexion of some of the generators and so on.

Power system automation. The development and inter-connexion of power systems makes it a more complicated matter to control their operating condition and it becomes more increasingly necessary to ensure the most economical operation of fuel-fired and hydro-electric power stations with network losses included. These problems can only be solved by automation.

Automation of the control of operating conditions in power systems cannot be confined to the installation of appropriate apparatus at despatcher control points. It is also necessary to apply automation to the execution of commands from the despatcher point to the station. Widespread automation of the thermal processes is also required.

The analysis of conditions has to be made by computers. A very promising line of development is the use of control machines for complex automation of boiler-turbine-generator units.

The most serious attention must be paid to the automation of transformer voltage control when under load, the connexion and disconnexion of capacitors, the distribution of reactive loads between power stations and power systems.

All this is very important from the point of view of improving the power supply.

Continuity of supply

Interruptions of the power supply are very important from the economic point of view. In order to prevent the disconnexion of important motors at the low voltages which accompany short-circuits in the networks and from automatic reclosure and the operation of standby devices, use should be made of self-starting for motors which is ensured by correct adjustment of the relay protection of the motors themselves and of the other elements in the network (generator, transformers, lines).

The choice of fuses is very important to ensure the self-starting of 380 V motors. They should be selected so as to ensure the disconnexion of the motor in the presence of inter-phase short-circuits and in the presence of line-to-earth faults in earthed neutral systems. In no case should they be used for protecting the motors from overloads, since this can lead to damage of the motor if the fuse blows on one phase and the motor operates on two phases.

Operating experience shows that the self-starting of synchronous motors is just as feasible as the self-starting of induction motors. Only in individual cases is it necessary to use a special device for the self-starting of synchronous motors. In exceptional cases it is also necessary to take away the excitation of synchronous motors for the automatic connexion of the standby supply and for automatic reclosure.

The scope for automation and remote control

Remote control and automation is not an end in itself, but only a means to achieve reliable and economical operation of the power system. If this is forgotten the volume of automatic equipment and remote control equipment in particular will be fixed without regard to actual operating conditions, the extent to which they are used and their costs, since this automatic equipment will be used to counteract fault conditions which become continually less and less likely. The capital cost of investment in automatic devices must always be justified most carefully. The investment must be recovered in a reasonable period of time out of operating costs.

It is not convincing to argue that the more important consumers require a larger amount of remote control equipment. Quite the contrary, such consumers who have a hundred per cent standby supply require least remote control equipment.

Unnecessary equipment is a waste of money and a nuisance, whereas its use on a sensible scale makes it possible to reduce the number of employees, to dispence with circuit breakers at substations on the high voltage side and so on. The correct choice of protection from short-circuits and overloads is very important for reducing the amount of automatic and remote control equipment.

Operating experience shows that in the overwhelming majority of cases a substation which is converted to operate without permanent staff need not be provided with remote control devices, telemetering equipment and remote indication equipment on a large scale. If automatic reclosure and automatic connexion of the standby supply is provided etc, it is sufficient to have a simple alarm system with two signals (fault and warning). In the Azerbaijan power system there are automatic substations on 20 and 35 kV which have operated for a long period without a permanent staff and without a system of remote indication and even though they are a large distance away from the base substation, the staff can be conveyed to the most outlying of them in about 20 min.

No permanent staff is necessary at stations without remote indication if the disconnexion of lines joining power system substations can be planned at substations where there is a permanent staff or when the onsumer himself can make the interruption known.

It is necessary to use automatic equipment on a large scale at those ubstations which supply industrial enterprises direct. But even the emoval of a permanent staff from these substations should not be inked with centralized control formed by an extensive system of autoation and remote control since the substations are quite close together. 'elemetering should only be used to a limited extent in the power sysems of industrial undertakings.

Operating experience shows that periodic inspection of substations ithout a permanent staff is quite sufficient. The number of inspecions at many substations have been reduced to one per week, and this s not the limit. Personnel are very rarely called to substations to liminate disturbances and faults. At many substations personnel are nly required during daytime and then not for operating the system, but nly to perform various operations such as maintenance and repair.

The main task is ensure reliable operation of power systems at inimum cost. This requires a reduction in the prime cost of electrial equipment and the reduction in operating costs. The following main easures have to be taken to achieve this in the field of relay protecion and electro-automation:

1. The production of unit-type devices and elements for electro-utomation and relay protection.

2. The reduction in dimensions of relays, apparatus and devices.

3. In the design and construction of new relay protection devices nd automatic equipment, special attention must be paid to the simpliication and improved reliability of the individual elements and of the evice as a whole, the reduction in the number of tests and checks during rection and use, and the elimination of internal inspection of the ndividual elements of a device.

4. Further improvement of substation systems without circuit breakrs on the high-voltage side and their automation.

5. The development of relay protection and automatic devices on perative a.c. for all voltages and outputs. Special attention must be aid to the reduction of consumption in the control circuits of circuit reakers, especially air-blast circuit breakers, the development and mprovement of electro-pneumatic, clockwork, and pendulum devices, and he widespread introduction of electromagnetic devices with rectifiers

and motor drives.

6. The application of semiconductors in relay protection and automation.

7. Industry should be geared to the production of devices and apparatus for testing the various types of device used in relay protection and power system automation.

Translated by O.M. Blunn

LONG DISTANCE POWER TRANSMISSION WITH D.C. MAGNETIZED REACTOR-TRANSFORMERS AND FORCED CAPACITOR BANKS*

D. I. AZAR'EV

Moscow

(Received 23 January 1960)

At the present time long distance power transmission of large current capacity is designed and equipped either with series-capacitor compensation for the inductive reactance of the line, or with synchronous condensers at intermediate substations. The power of the compensating devices may be very large. For example, on the 400 kV line between the Volga hydro-electric station and Moscow the power of the capacitor banks is 485 MVA, and the power of the reactors for compensating the excess reactive power at minimum loads and limiting the voltage rise on connexion of the line its 750 MVA.

The power of the compensating devices increases with distance and voltage. For example, a single-circuit 650 kV line of 2500 km and 2100 MW requires static capacitors with a total power 4700 MVA and reactors with a total power of about 4000 MVA, i.e. each kilowatt of transmitted active power requires about 2.2 kVA of static capacitors and about 2 kVA of reactors. If the transmitted power is increased from 2100 to 2400 MW, then the power of the static capacitors increases from 2.2 to 2.7 kVA per 1 kW of transmitted power.

It has been shown by a study of long distance transmission with series compensation that the power of static capacitors is completely or partially absorbed by the reactors even if maximum power is transmitted. It is only possible to disconnect the reactors if the degree of series compensation leads to a reduction in the margin of stability. One escape from this situation would be the use of synchronous condensers at intermediate substations. However, this requires the use of transformer substations and, consequently, additional capital outlay. In addition, synchronous condensers do not allow large overloads and active losses are considerable.

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* *Elektrichestvo*, 7, 24-30, 1961.

The author of this paper has already suggested [1,2] that synchronous condensers be replaced by reactors with magnetization and forced capacitor banks with programme control. In this system use may be made of the low voltage reactor proposed by M.S. Libkind [3], or any other high-speed reactor with magnetization, but a still better system can now be used.

It is possible to produce a unit in which a transformer and an automatically controlled high-voltage reactor are combined and which is so quick-acting that it is capable of reducing overvoltages and increasing the current capacity of the line so that the cost of transmission is reduced and less equipment is required [4].

It is not possible to use a control reactor in the form of a conventional transformer [5] because the shape of its current and voltage curve is considerably distorted, and there is a large time constant under changing conditions which exceeds 10 sec. The 20 kV [6] reactor proposed by Friedlander possesses a curve of acceptable shape, which can be produced at higher voltages and used as the transformer if a secondary winding is attached to it, however, the direct magnetic link between the d.c. control winding with the closed delta winding does not allow high-speed control of the reactor (time constant of the magnetization winding is 0.75 sec). The six-core magnetic system requires a large amount of material, more than three times that of a non-controlled reactor.

Better results can be obtained if the magnetic system of the reactor is split into parallel branches magnetized by d.c. in such a way that the magnetic fluxes are in opposite directions [4]. A reactor can be constructed on this principle if use is made of the magnetic system of conventional three-phase or one-phase transformers. The rate of control of such a reactor will be higher since the magnetic flux created by the d.c. winding induced currents in the a.c. windings.

The magnetic system can be split in a different way, for example, as shown in Fig. 1. The reactor transformer in Fig. 1 consists of a three-core magnetic system 1, which has two zig-zag connected primary high-voltage windings 2 and 3, and a secondary winding 4 connected in delta.

The secondary winding is used for connecting the capacitor banks and for supplying local consumers. It also serves to eliminate the third armonic. More remote consumers can be supplied from windings 3 or 2.

Magnetization winding 5 is disposed on magnetic circuits 6 and 7, hich adjoin the faces of the three-core magnetic system, and is conected in such a way that the magnetic fluxes produced by it are closed through cross-pieces 8 of the three-core magnetic system and do not enetrate into the cores of this system on which the a.c. windings are isposed. With this connexion of the magnetization winding the control speed of the reactor is higher than that of the reactor proposed by riedlander [6].

In order to eliminate the fifth and seventh harmonics, the primary indings of another but similar reactor transformer must be connected so that the phase sequence is reversed (A-C-B instead of A-B-C).

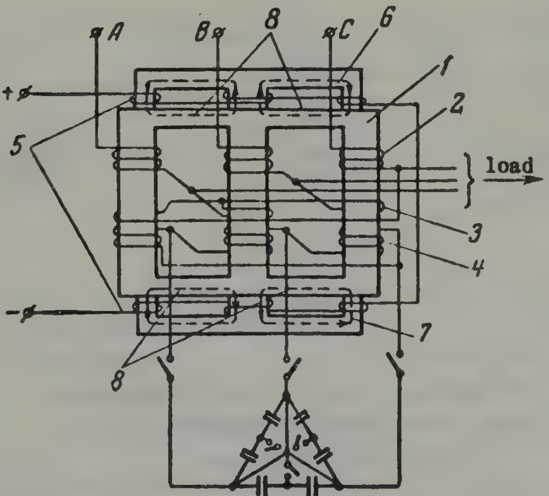


Fig. 1. Circuit of the magnetizable reactor-transformer with forced capacitor bank.

In order to check the shape of the curve, tests have been carried out by Teploelektroproekt on a prototype reactor-transformer. The tests showed that quite an acceptable shape of curve for the current and voltage can be obtained. Fig. 2 shows oscillograms of the linear current of a reactor transformer and the total current of two reactor transformers with identical magnetization.

The flow of d.c. through the magnetization windings saturates the cross-pieces 8 and the magnetic circuits 6 and 7, and their magnetic permeability is reduced.

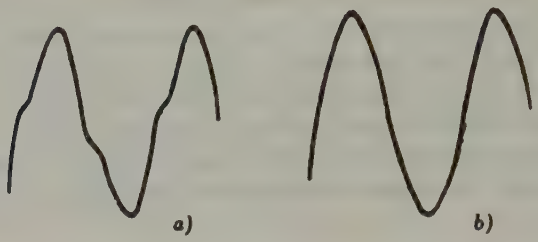


Fig. 2. Oscillograms of the line current of a reactor-transformer (a), and the total current of two reactor-transformers (b).

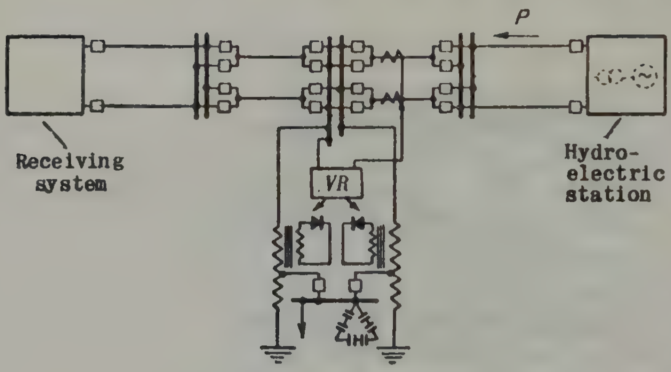


Fig. 3. Method of long distance transmission with magnetizable reactor transformers and forced capacitor banks.

Since the fluxes created by the a.c. windings pass through the same parts of the magnetic system, the inductive reactance of these windings is reduced, and the current increased.

By varying the magnetization current, the reactive power absorbed by the reactive-transformer can be varied by a factor of 10. This makes it possible to connect the reactor transformers without circuit breakers since on heavy loads the consumed or reactive power can be reduced by decreasing the magnetizing current, and if faults occur it is possible to disconnect the system of bus-bars to which it is connected.

The use of magnetized reactor-transformers is expedient not only for increasing the current capacity of the line, but also for limiting the voltage in the absence of load and interruptions and connexions at one end. In this latter case reactors with magnetization are more effective than reactors with a linear volt-ampere characteristic, since the reactive power consumed by them increases rather more quickly with increasing voltage.

The efficiency of the proposed system (Fig. 3) will now be considered. The 400 kV line between the Volga hydro-electric station and Moscow will be used as an example.

Series-compensation of the inductive reactance of the line between the Volga hydro-electric station and Moscow (Fig. 4) is provided by static capacitors with a total power of 485 MVA, a total resistance 32 Ω, and a rated current 2250 A.

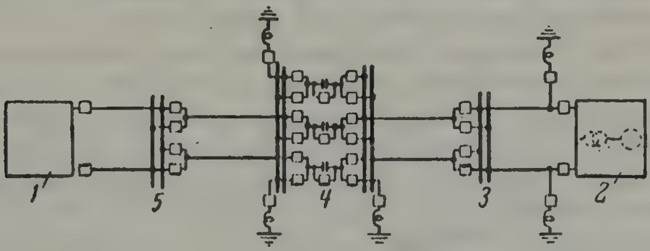


Fig. 4. Transmission system between the Volga hydro-electric station and Moscow. *1* – receiving system; *2* – Volga hydro-electric station; *3* – switching point No. 1; *4* – switching point No. 2; *5* – switching point No. 3.

Under normal conditions the current load of the capacitors is 79 per cent. If one of the circuits of some section of the line is interrupted, one of the capacitor banks is also disconnected and the two banks which remain in operation are loaded to 118.5 per cent. In order to absorb the excess reactive power at the switching point 2, a set of three reactors is installed with a total power of 450 MVA. The number of 400 kV circuit breakers at point 2 is 18.

To keep the direct-axis transient e.m.f. constant and equal to its value under nominal conditions, the maximum transmitted power is 1500 MW. If excitation regulators are used so that the voltage on the generator bus-bar is constant, the maximum power is 1640 MW, and if regulators are used to keep the voltage on the bus-bars of 400 kV stations constant, maximum power is 1840 MW. If the margin of stability is kept the same at 15% for all cases, the transmission of power under normal conditions is 1300, 1420 and 1600 MW respectively.

If it is considered that under normal conditions a load of the static capacitor bank up to nominal current and nominal voltage is permissible, then the transmission of the specified outputs requires static capacitors with a total power of 306, 367 and 466 MVA respectively. In this case, if one of the parallel circuits of some section of the line is interrupted, then one of the three static capacitor banks is disconnected and the other capacitors will be overloaded by a factor of 1.5 as regards current, and 1.5 as regards voltage, i.e. by a factor of 2.25 as regards power. The curves shown in Fig. 5 show that for the case when E'_d of the generators is constant, the same maximum output (1500 MW) can be transmitted if use is made of transverse (shunt) compensation in place of series compensation and reactors with a linear volt-ampere characteristic, where transverse compensation is in the form of static capacitors and reactors with magnetization. In the following we will refer to transverse compensation as shunt compensation as opposed to series compensation which can alternatively be called longitudinal compensation. However, in the case of shunt compensation the necessary power is considerably less. For example, in order to obtain the same margin of stability as in the case of series compensation, the reactive power required in the middle of the line is 240 MVA when transmitting a power of 1300 MW, and the source of reactive power in the middle of the line must give 110 MVA when transmitting the maximum power 1500 MW.

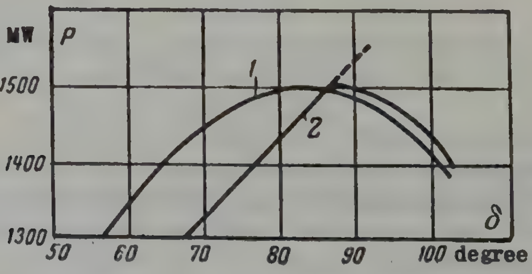


Fig. 5. The transmitted active power as a function of the phase angle of the e.m.f. of the transmitting station with E'_d of the generators constant: 1 – with series compensation; 2 – with shunt (transverse) compensation.

Assuming that the reactor requires 40 MVA in the absence of magnetization current under maximum conditions, it is found that the power of the static capacitors $Q_{s.c} = Q_1 + Q_{1r} = 110 + 40 = 150$ MVA. The power consumed by the reactor under normal conditions is $Q_{2r} = Q_2 + Q_{s.c} = 240 + 150 = 390$ MVA (Q_1 and Q_2 stand for the reactive power consumed or delivered by the plant under normal and maximum conditions). The power consumed by the reactor needs to be varied by a factor

$$k_n = \frac{Q_{2r}}{Q_{1r}} = \frac{390}{40} \approx 10.$$

Under fault conditions the source of reactive power in the middle of the line must deliver 110 MVA when transmitting 1300 MW and 350 MVA when transmitting 1500 MW. Consequently, the static capacitors must deliver a power $Q_{s.c} = 350 + 40 = 390$ MVA; when transmitting 1300 MW the reactor must consume the power $Q_r = 390 - 110 = 280$ MVA. The power consumed by the reactor needs to be changed by a factor

$$k_f = \frac{280}{40} = 7.$$

If it is assumed that the capacitor banks are forced as regards power by a factor 2.25 the same as in series compensation, the established power of the capacitor banks must equal 173 MVA.

In order to obtain the same stability margin with a constant voltage on generator bus-bars as in the case of series compensation, it is necessary that when transmitting 1420 MW the reactive power in the middle of the line has to be 80 MVA, and when transmitting the maximum power 1640 MW 225 MVA should be delivered.

Taking the same minimum power of the reactor as in the previous case, the power of the static capacitors $Q_{s.c} = 225 + 40 = 265$ MVA, and the maximum power of the reactor $Q_r = 265 + 80 = 345$ MVA. Consequently, the necessary change in output is

$$k_r = \frac{345}{40} = 8.65.$$

Under fault conditions it is necessary that the source of reactor power in the middle of the line should deliver 330 MVA when transmitting 1420 MW and 490 MVA when transmitting 1640 MW. Likewise, the static capacitors under these conditions must deliver 530 MVA, i.e. they have to be forced as regards power by a factor of 2, and the reactor must increase its output from 40 to 200 MVA, i.e. by a factor of 5.

If the direct-axis transient e.m.f. is constant as well as the voltage on the bus-bars of the generator, the power of the static capacitors and reactors can be decreased still further, if a slight reduction is allowed in the voltage in the middle of the line under maximum conditions as is the case in series compensation. For example, with a constant generator voltage, a 5 per cent reduction in voltage gives the results shown in Table 1.

TABLE 1

Conditions	Transmitted power, MW	Voltage in middle of line	Neccessary reactive power in middle of line, MVA	Reactor load, MVA	Load of static capacitor bank, MVA
Normal	1.420	U_{nom}	+80	+235	−155
	1.640	$0.95\ U_{nom}$	−100	+40	−140*
One circuit disconnected on one section of line	1.420	U_{nom}	−330	+45	−375
	1.520	$0.95\ U_{nom}$	−300	+40	−340*

* The power of the static capacitors is reduced 10% owing to the 5% reduction of voltage under limiting conditions.

Assuming that the capacitor banks under fault conditions are forced as regards power by the same factor as in series compensation, i.e. by a factor of 2.25, the rated power of the banks will be 166 MVA. The maximum power consumed by the reactors with magnetization is 235 MVA, and the necessary power variation factor is 5.88. In order to obtain the same margin of stability as series compensation it would be necessary to have static capacitors with an output of 367 MVA, i.e. 2.2 times greater.

The reason for the greater efficiency of the capacitors in the proposed system is that with series compensation the excess reactive power of the static capacitors is absorbed by the reactors which remain connected under maximum conditions. However, in the proposed system the reactors operate without magnetization under maximum conditions and, without reducing the voltage, consume considerably less power. Under fault conditions with series compensation, some of the capacitors are disconnected for purposes of forcing and are not used, even though they are then more necessary. In the proposed system all the capacitors remain connected.

It should be pointed out that with series compensation the static capacitors operate under rather less favourable conditions then in shunt compensation. For example, with series compensation the voltage on the capacitors during short circuits and swings can be increased by a factor of 2-2.5 or more with an increase in current. In the proposed system under normal conditions the voltage on the capacitors does not exceed the rated voltage, and under fault conditions it is increased only by a factor of 1.5 with forcing of the capacitor banks as regards power by a factor of 2.25.

In the proposed system the capacitor banks are connected to a lower voltage which makes it possible to reduce the cost of the equipment and improve its operating conditions.

An important advantage of magnetized reactors is that with an increase in voltage the power consumed by them is increased more sharply than with reactors having a linear volt-ampere characteristic. Consequently, they limit overvoltages more effectively when some lines are interrupted.

The effect of the proposed system on dynamic stability will now be considered.

The dynamic stability of a power station in a large network is the greater, the greater the area bounded by the curve $P_g - P_t = f(\delta)$, where P_g and P_t are respectively the powers developed by the generator and turbine. It will be seen from Fig. 6 that with the same margin of static stability the area bounded by the curve $P = f(\delta)$ and the straight line P_n is slightly less with shunt compensation than with series compensation. This is explained by the fact that at the moment maximum active power is achieved the magnetization is reduced to zero and the reactor is in no state to maintain the voltage in the middle of the line. In order that the area should be the same in both cases, it is necessary to increase the forcing of the capacitor banks at the first moment.

It has been shown by calculations that if the bank of static capacitors is forced as regards power by a factor of 2.25 for a prolonged period, then they must be forced by a factor of 3 on occasion in order to maintain dynamic stability, i.e. the bank voltage must be increased by approximately 1.73 compared with nominal voltage. It should be mentioned that even with series compensation the current and voltage overload of the bank exceeds 1.5 as a result of generator swings.

It will be seen from Fig. 7 that with generator swings the relative angle between the e.m.f. of the Volga hydro-electric station and the receiving system with shunt compensation is greater than with series compensation. However, this is of no importance since the maximum permissible angle in shunt compensation is greater than in series compensation. The maximum transmissible power with a constant voltage in the middle of the line for shunt compensation is inferior if the angle between the voltages at the ends of the transmission line is 180° and for series compensation at the angle 90°. This difference is the less, the greater the voltage reduction in the middle of the line.

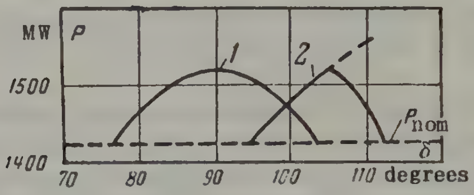


Fig. 6. The transmitted active power as a function of the phase angle of the e.m.f. of the generators at the Volga hydro-electric station under fault conditions.
1 – series compensation; *2* – shunt (transverse) compensation. The broken line corresponds to the increased power of the bank of static capacitors.

Forcing of the capacitor banks is necessary when it is necessary to transmit power equal to power under normal transmission conditions when one circuit of the line on some section is interrupted.

If this is not necessary, then both static and dynamic stability can be ensured by disconnecting some of the generators at the transmitting power station under fault conditions. In order to improve the dynamic stability, use may also be made of electrical or mechanical braking for the generators. If it is necessary to transmit the same power as under normal conditions when one circuit of some section is interrupted, then the use of braking does not exclude the forcing of the capacitor banks.

It has been shown by calculations for two values of the time constant of the reactor resistance (1 and 0.2 sec), that this has no great effect on stability.

The use of magnetized reactors is also possible for series compensation of the inductive reactance of the line, but such a combination is less expedient since it requires a larger power for the capacitor banks. The cost of equipment for series compensation with magnetized reactors will be higher because the capacitors are connected to a higher voltage and require additional high-voltage circuit breakers. It should however be noted that in Table 2 it is assumed that the cost of capacitors for series and shunt compensation is the same.

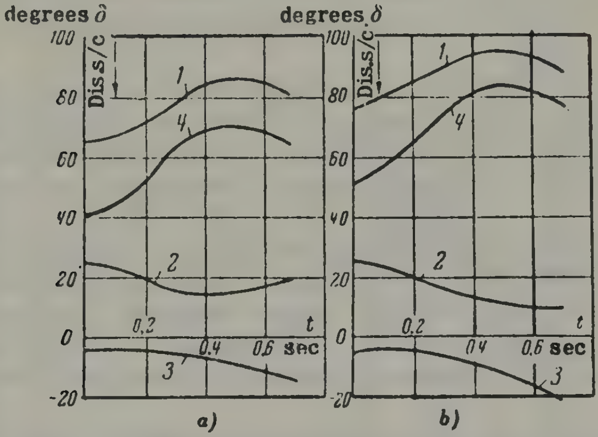


Fig. 7. Swinging of generators due to two-line-to-earth faults with series (a) and shunt compensation (b)

1 – phase angle of e.m.f. of generators at the Volga hydro-electric station; 2 – phase angle of receiving system; 3 – phase angle of synchronous condensers; 4 – relative angle between the e.m.f. of the Volga hydro-electric station and the e.m.f. of the receiving system.

It has been shown from calculations that if magnetized reactors were installed on the transmission line between the Volga hydro-electric station and Moscow, the same limit of static stability can be achieved with a resistance of the capacitor banks equal to 23.7 Ω, i.e. the power of the capacitor banks can be reduced from 367 to 266 MVA, or by a factor of 1.37. In the proposed system however, the power of the capacitor banks is equal to 155 MVA, i.e. approximately 1.5 times less.

Moreover it has been calculated that with a resistance of the capacitor banks of 23.7 Ω under normal conditions and 34.6 Ω in post-fault conditions dynamic stability is not ensured.

TABLE 2

Type of equipment	With series compensation			With intermediate synchronous condensers			With magnetizable reactor-transformers		
	Quantity*	Cost		Quantity*	Cost		Quantity*	Cost	
		Units**	Total, mill. roubles		Units**	Total, mill. roubles		Units**	Total, mill. roubles
Capacitors	367	85	31.2	62	85	5.3	155	85	13.2
Reactors	450	15	6.7	–	–	–	235	30	7.0
Circuit breakers	18	3	54.0	8	3	24.0	8	3	24.0
Transformers	–	–	–	330	20	6.6	–	–	–
Synchronous compensators	–	–	–	380	75	28.4	–	–	–
TOTAL	–	–	91.9	–	–	64.3	–	–	44.2

* Quantity of capacitors, reactors, transformers and synchronous condensers is given in MVA, but circuit breakers in a number of items.

**Cost of capacitors, reactors, transformers and synchronous condensers is given in roubles/MVA, but circuit breakers in mill, roubles per unit.

The overloading of the capacitor banks in series compensation is also related to the increase in current which takes place during swings and after the capacitors are disconnected. If the swings are large, the current after disconnexion of the capacitors can be twice the current under normal operating conditions of the line or three times greater than the nominal current. However, if the capacitors are connected to the secondary winding of the reactor transformer, the maximum voltage on the capacitors will depend on a pre-determined degree of forcing and is relatively independent of transmission conditions.

It is to be expected that the use of high voltage high-speed reactor transformers will make it possible to reduce overvoltages in lines and reduce insulation requirements and, consequently, either reduce their cost or permit an increase in voltage. The latter is the more rational.

In the first period of operation of new lines, large power is often not required and the reactor transformers will often be magnetized, i.e. used mainly as reactors. Later, when the load has increased, they will be used mainly as transformers; if there is a shortage of reactive power the capacitor banks will often be connected to them.

A comparison will now be made between the proposed system of transmission and the system using synchronous condensers.

It will be seen from Table 1 that if some circuit of one line section is disconnected and it is necessary to transmit 1420 MVA, then the source of reactive power in the middle of the line must deliver 330 MVA into the line. In order to deliver this power, it is necessary to have a 330 MVA transformer. The loss of reactive power in it will be at least 50 MVA. Consequently, all the synchronous condensers must deliver 380 MVA which must be their nominal power since a sustained overload of the condensers cannot be allowed.

In order to increase the dynamic stability, it is necessary to compensate the reactance of the transformer and the transient reactance of the synchronous condenser. If it is assumed that the first is equal to 17 and the second to 34 per cent, then 90 per cent compensation requires 140 MVA of static capacitors. Allowing for an overload of 2.25, 62 MVA will be sufficient.

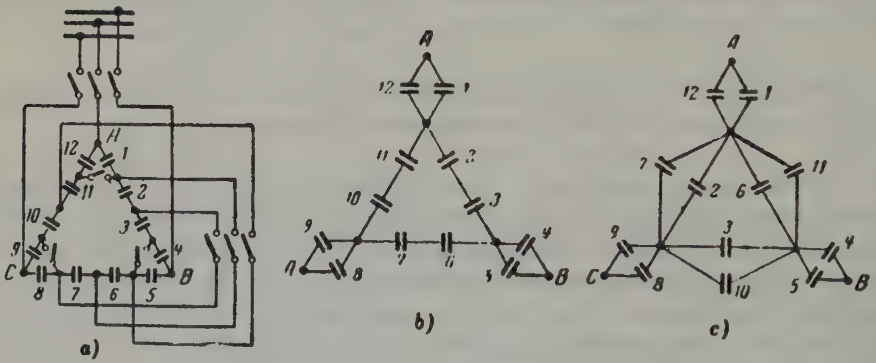
Table 2 compares the amount and cost of the equipment for series compensation, synchronous condensers and the proposed system. The cost of erecting the equipment is included.

It will be seen from Table 2 that the system with magnetized reactor transformers requires considerably less equipment and its cost is accordingly lower.

The loss of active power in the circuit with synchronous condensers is slightly larger than in systems with magnetized reactor transformers and with series compensation.

For example, on full load the loss of active power in synchronous condensers of KSV-75,000 type is about 1.4 per cent of their nominal power, the transformer loss is about 0.5 per cent and the total loss 1.9 per cent. However, the losses in a magnetized reactor transformer on full load is about 1 per cent. The saving on annual losses is about 1.5 million rubles.

Thus, the system with magnetized reactor transformers is considerably cheaper than systems with synchronous condensers or systems with series compensation in particular.



a) b) c)

Fig. 8. Method of forcing capacitor banks (forcing factor from 1 to 4).

The capacitor banks connected to the reactor transformer can be forced in different ways, e.g. by switching to a previously provided system of branching the higher voltage [1], or by the system illustrated in Fig. 8 [7]. In the latter case, by changing the reactance of the bank x_C, any degree of forcing can be obtained.

$$k = \frac{1}{x_C + 0.25}.$$

If $x_C = 0$, the degree of forcing as regards power is 4, and if $x_C = 0.5$, it is 1.33.

It should be borne in mind, that with a degree of forcing other than 1.33 or 4, the capacitors are not charged uniformly. In order to distribute the charge more uniformly, it is necessary under these conditions to select the appropriate rated currents of the bank. If the degree of forcing is 1.33 or 4, the distribution of the charge is uniform when the capacitors are switched into the double star system (Fig. 9b) or into the double triangle system (Fig. 9c).

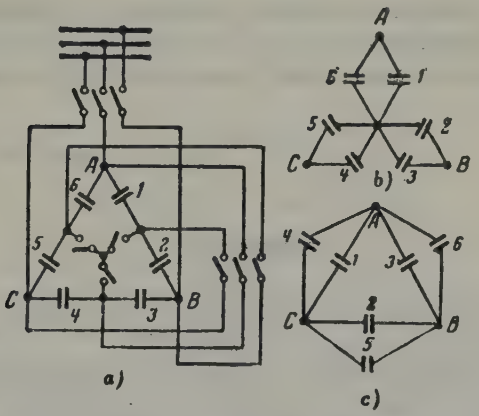


Fig. 9. Method of forcing capacitor banks (forcing factor 1.33 and 4).

Other means of forcing the capacitor banks have been suggested, such as the use of control valves which are connected in series with the capacitor banks [8].

There is also a suggestion to split the magnetic system of the magnetized transformer in the core [9] instead of in the yoke as considered in this paper. Further investigation is required to determine which system is the best. Such an investigation is outside the scope of this paper.

Thus, the proposed system of long distance transmission with magnetized reactor transformers and forced capacitor banks [4] has the following advantages:

1. It is more quick-acting than the system with synchronous condensers and ensures the maintenance of the voltage at intermediate points in the power systems and thereby increases the current capacity and stability of the transmission system.

2. It incorporates no rotating machines and is therefore more reliable

and cheaper to run.

3. It requires fewer capacitors and high-voltage circuit breakers than the system with series compensation.

4. It limits overvoltages and permits a reduction in the level of insulation on lines and equipment directly connected to it.

5. It makes it possible to supply consumers in adjoining regions immediately after installation and at the same time provides maximum current-capacity without additional expenditure on equipment.

Translated by O.M. Blunn

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THE PERMEANCE OF MAGNETIC CIRCUITS WITH TEETH*

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Use is often made in various applications of the type of magnetic system with teeth illustrated in Fig. 1. In such systems there is quite a large leakage flux which can in some cases be of the same order as the operating flux and even exceed it. It is therefore very important to be able to determine the magnitude of the total magnetic permeance of such systems. Naturally, no rigorous solution of this problem exists owing to the complexity of the boundary conditions and the need to calculate a three-dimensional field. It is therefore necessary to consider an approximate method. In this paper a method is described which is based on a particular idealization of the field of the system.

The proposed method of analysing the permeability of magnetic systems with teeth is based on a conditional division of the space around the pole ends into separate regions. Given the geometric shape of the poles, it is expedient to consider the field between the faces and the field between the ribs separately. If the field between the faces is conditionally assumed to be two-dimensional (since a two-dimensional field is the simplest to analyse) the permeance calculated on the basis of this assumption will be less than the true value since the edge (surface) effect is ignored. This difference between the true magnetic permeance and the approximate value can be included by introducing an additional permeance as a correction for the surface effect). The author has found an analytic expression which defines the additional permeance as a function of the main geometric criteria of the system as a result of the investigation of experimental data.

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* *Elektrichestvo*, 7, 59-63, 1961.

The total permeance of the system under consideration can therefore be defined as the sum of the permeance G_1 between the external faces, G_2 that between the internal faces and G_3 the additional permeance.

Permeance will now be found for the general system in which tooth is above tooth (see Fig. 1).

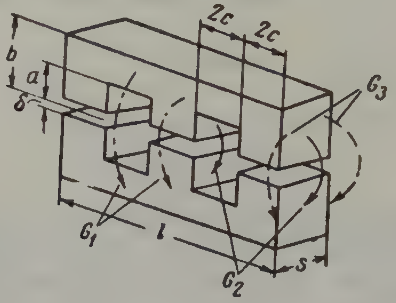

Fig. 1. Tooth pole ends

The permeance G_1 is defined by the formula [1]

$$G_1 = \mu_0 \left[n \int_0^{2c} \varphi(k_{11})\, dx + (n-1) \int_0^{2c} \varphi(k_{12})\, dx \right] = \mu_0 [n\varphi(k_{11}) + (n-1)\varphi(k_{12})]\, 2c, \qquad (1)$$

where μ_0 is the magnetic permeance of air; and

$$\varphi(k_{11}) = \frac{K(k'_{11})}{K(k_{11})}; \quad \varphi(k_{12}) = \frac{K(k'_{12})}{K(k_{12})}$$

are functions representing the relationship between the additional and total elliptic integrals of the first kind with moduli respectively equal to (see appendix I):

$$k_{11} = \frac{\delta}{b}; \quad k'_{11} = \sqrt{1 - k_{11}^2}; \quad k_{12} = \frac{a}{b}; \quad k'_{12} = \sqrt{1 - k_{12}^2};$$

2δ is the size of the air-gap, $2c$ the width of the tooth or recess (this relationship between the size of the tooth and recess is taken for the sake of simplicity, but in general any dimensions can be used by ppropriately changing the limits of integration in formula 1), and n he number of teeth ($n - 1$ is the number of recesses).

The permeance G_3 is defined by the formula [1]:

$$G_3 = \frac{b}{30}\left[\frac{2\tanh 0.87p}{k_{11}} + 41\tanh 5.5p\right]\mu_0, \tag{2}$$

here $p = \frac{l}{b}$.

As a rule $p \gg 1$, and so eqn. (2) simplifies to:

$$G_3 = \frac{b}{30}\left[\frac{2}{k_{11}} + 41\right]\mu_0. \tag{2a}$$

The determination of G_2 can be considerably simplified if it is ccepted that in real systems $\delta \ll c$. It is therefore quite accurate o suppose (see appendix II) that the field between the teeth is uniform nd, consequently, the bulging of the lines of force at the edges of the ooth can be ignored. This allows the use of conformal transformations o determine the permeance of the space between the recesses. Using hristophel-Schwartz' integral, the rectangular $CDEF$ (the plane z) can e represented as a transformation of the upper half-plane ζ (Fig. 2). orrespondence of the points can be selected as follows:

$$0 \to 0; \quad E \to c\,(F \to -c); \quad M \to \infty.$$

The function for this transformation has the form:

$$z = A\int_0^{\zeta}\frac{d\zeta}{\sqrt{(\zeta^2 - c^2)(\zeta^2 - f^2)}}, \tag{3}$$

here f is the co-ordinate of the plane ζ, which corresponds to the o-ordinate of the point C in the plane z.

In order to determine A and f, use is made of the following condi-ions:

$$z = c \text{ if } \zeta = c \text{ and } z = c + j2a \text{ if } \zeta = f,$$

which, if substituted in eqn. (3), give after simple transformation 2]:

$$A = \frac{cf}{K(k_{11})}; \tag{4a}$$

$$\frac{2a}{c} = \frac{K(k'_{21})}{K(k_{21})} = q, \tag{4b}$$

where $K(k'_{21})$ and $K(k_{21})$ are total and additional elliptic integrals of the first kind.

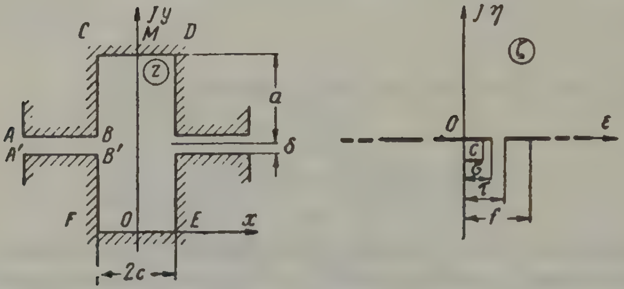

Fig. 2. Original system and system in transformed plane.

The moduli of the elliptic integrals k_{21} and k'_{21} can be found either graphically or by equations 4a or 4b, or by the approximate formulae:

$$k_{21} = \frac{c}{f} = \frac{4}{e^{\frac{\pi q}{2}}}\bigg|_{q>1.83} ; \quad k_{21} = \frac{c}{f} = \frac{2.035 - q}{1,45}\bigg|_{q<1.83} ;$$

$$k'_{21} = \sqrt{1 - k_{21}^2} .$$

In order to determine the co-ordinates τ and σ, which correspond to the co-ordinates of the points B and B', use is made of the conditions:

$$z = c + j(a - \delta) \text{ if } \zeta = \sigma;$$

$$z = c + j(a + \delta) \text{ if } \zeta = \tau,$$

which in conjunction with eqn. (3) give:

$$K(k_{21})\frac{a-\delta}{c} = F(\varphi_{21};\ k'_{21}); \tag{5a}$$

$$K(k_{21})\frac{a+\delta}{c} = F(\varphi_{22};\ k'_{21}), \tag{5b}$$

where

$$F(\varphi_{21};\ k'_{21});\ F(\varphi_{22};\ k'_{21})$$

are partial elliptic integrals of the first kind with the modulus k'_{21} and the amplitudes φ_{21} and φ_{22}.

The amplitudes φ_{21} and φ_{22} can be found from Tables of the values $F(\varphi;\ k'_{21})$, i.e. from the values of the left-hand sides of eqn. (5).

The following formula is used to determine the permeance per unit length of the upper-half space of a system of two semi-infinite and one finite plate, which corresponds to the permeance of the recess (see appendix I):

$$G''_2 = \frac{K(k_{22})}{2K(k'_{22})}\mu_0, \tag{6}$$

where $K(k_{22})$ and $K(k'_{22})$ are total elliptic integrals of the first kind with moduli:

$$k_{22} = \sqrt{\frac{f^2-(f^2-c^2)\sin^2\varphi_{22}}{f^2-(f^2-c^2)\sin\varphi_{21}}};\quad k'_{22} = \sqrt{1-k^2_{22}}.$$

Consequently, the permeance between the teeth is defined in the same way as for a uniform field:

$$G'_2 = \frac{c}{\delta}\mu_0. \tag{7}$$

The total permeance between the internal faces of a system consisting n teeth therefore equals:

$$G_2 = \left(\frac{K(k_{22})}{2K(k'_{22})} + \frac{c}{\delta}\right) ns\mu_0, \tag{8}$$

where S is the width of a side face.

In order to determine the permeance for other systems with the teeth in other positions, use may be made of the relationships obtained by Sribner [3], which have been confirmed quite well by a more accurate analysis.

Tooth above recesses:

for a single nucleus

$$G_2 \approx G_2'' 4s;$$

and for the whole system

$$G_2 \approx 2G_2''(2n - 1)\, s.$$

Half of tooth above half of recess:

for a single nucleus

$$G_2 \approx \left(2G_2'' + \frac{c\mu_0}{2\delta}\right) s;$$

and for the whole system

$$G_2 \approx \left(4G_2'' + \frac{c\mu_0}{2\delta}\right) ns.$$

Formulae can also be written in the same way for the permeance G_1

Tooth above recess:

$$G_1 = 4\varphi(k_{13})(n - 1)\, c\mu_0;$$

$$k_{13} = \frac{a + \delta}{2b - a + \delta}; \quad k_{13}' = \sqrt{1 - k_{13}^2}.$$

Half of tooth above half of recess:

$$G_1 = [n\varphi(k_{11}) + 2(n - 1)\varphi(k_{13}) +$$
$$+ (n - 1)\varphi(k_{12})]\, c\mu_0.$$

The total permeance of the system under consideration for any position may be defined as the sum:

$$G = G_1 + G_2 + G_3.$$

The results obtained in this paper have been confirmed experimentally (see appendix III).

Appendix I. In order to determine the permeance (per unit of length) of two systems of plates consisting of two upper plates of finite width and two lower semi-infinite plates and one finite plate (Fig. 3), it is necessary to effect a conformal transformation of the half-plane ζ on the rectangle $AA'B'B$ in the plane z such that the following correspondence of points obtains:

$$\begin{gathered} A \to \alpha;\ A' \to \alpha'; \\ 0 \to 0; \\ B \to \beta;\ B' \to \beta'; \\ C \to \infty \end{gathered}$$

Such a transformation can be obtained by Christophel-Schwarz' integral, which in this case in expressed by the relationship

$$z = A\int_0^{\zeta} \frac{\alpha}{\sqrt{(\zeta^2-m^2)(\zeta^2-n^2)}}. \tag{I-1}$$

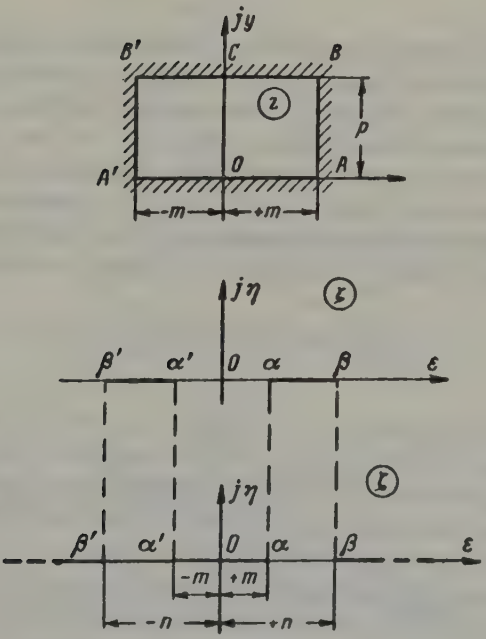

Fig. 3. Analysis of permeance between plates.

Substituting $t=\zeta/n$ equation (I-1) takes the form:

$$z = A\int_0^{t} \frac{dt}{\sqrt{(1-t^2)(1-k_{10}^2 t^2)}}, \tag{I-2}$$

where

$$k_{10}=\frac{m}{n}.$$

The constant A is found from the condition

$$z=m \text{ if } \zeta=m$$

and is equal to

$$A=\frac{m}{K(k_{10})},$$

where $k(k_{10})$ is the total elliptic integral of the first kind with the modulus k_{10}.

After performing these preliminary calculations, there is no difficulty in determining the required permeance. In fact, if the upper or lower system of plates is given, this implies that the co-ordinates of the points α and β are also given (and so are α' and β' by virtue of symmetry). By virtue of the assumed correspondence of points, point α goes into the plane z at point A, and β at B. Consequently, it only remains to determine the co-ordinate point B by the expression (I-2) and B' accordingly). This co-ordinate is found from the condition

$$z=m+jp \text{ if } \zeta=n,$$

i.e.

$$\begin{aligned} z=m+jp&=\frac{m}{K(k_{10})}\int\limits_0^{\frac{n}{m}}\frac{dt}{\sqrt{(1-t^2)(1-k_{10}^2t^2)}}=\\ &=\frac{m}{K(k_{10})}\left[K(k_{10})+\int\limits_1^{1/k_{10}}\frac{dt}{\sqrt{(1-t^2)(1-k_{10}^2t^2)}}\right]. \end{aligned} \tag{I-3}$$

Substituting

$$1-k_{10}^2t^2=(k_{10}')^2u^2,$$

where

$$k_{10}'=\sqrt{1-k_{10}^2},$$

it is found that the second integral in eqn I-3 is equal to $K(K'_{10})$. Consequently,

$$\left.\begin{aligned} m + jp &= \frac{m}{K(k_{10})}\,[K(k_{10}) + jK(k'_{10})]; \\ p &= \frac{K(k'_{10})\,m}{K(k_{10})}. \end{aligned}\right\} \qquad \text{(I-4)}$$

It is easy to see that the initial upper and lower systems of plates in the plane z (the rectangle $AA'B'B$) are transformed into a simple system which is an ideal plane capacitor.

The permeance of such a system is:

for the upper system

$$G_u = \mu_0 \frac{p}{2m};$$

for the lower system

$$G_l = \mu_0 \frac{2m}{p}.$$

Substituting the value of p from eqn. I-4 in these formulae,

$$G_u = \mu_0 \frac{K(k'_{10})}{K(k_{10})}; \quad G_l = \mu_0 \frac{K(k_{10})}{2K(k'_{10})}.$$

Appendix II. Suppose it is required to estimate the error which occurs in determining permeance on the assumption that the field between the teeth is uniform. It is expedient to make this estimate for a system in which the permeance can be determined exactly and then the discrepancy between this exact permeance and the permeance calculated as for a uniform field will be maximum. Such a system is one which consists of two teeth (Fig. 4). In point of fact the bulging of the lines of force in the region between the teeth in such a system will be considerably greater than in a system consisting of a number of teeth. This decrease in the effect of bulging of the lines of force in systems with several teeth can be explained by the compressive effect of the flux from adjacent teeth. The permeance between the face surfaces of a system consisting of two teeth is defined by the formula [1]:

$$G_f = \frac{2\mu_0}{\pi} \ln \frac{1 + \sqrt{1 - k^2}}{k} \qquad \text{(II-1)}$$

where $k = f\left(\frac{m}{h}\right)$ is the modulus of the elliptic integrals in the equation

$$p = \frac{m}{h} = \frac{K(k') - E(k')}{E(k)}; \quad (k' = \sqrt{1 - k^2}). \tag{IJ-2}$$

Eqn. (II-2) can be solved graphically. It is of practical interest to consider cases of systems for which $p > 5$. This corresponds to a very small value of k. For small values of k eqn. II-2 has the following approximate solution:

$$k = \frac{4}{e^{\frac{\pi}{2}p+1}}. \tag{II-3}$$

The required error is

$$\Delta = \frac{\Delta G_1}{G_f} = \frac{G_f - G_0}{G_f}.$$

where $G_0 = \mu_0 \frac{m}{h}$ is the permeance between faces calculated on the assumption that the field between the teeth is uniform.

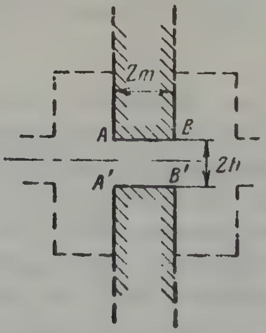

Fig. 4. Two-pole system.

Thus, the formula for the error can be written in the form:

$$\Delta = \frac{\frac{2}{\pi}\ln\frac{2}{k} - \frac{m}{h}}{\frac{2}{\pi}\ln\frac{2}{k}} = \tag{II-4}$$

$$=\frac{\frac{2}{\pi}\ln 2-\frac{2}{\pi}\ln 4+\frac{2}{\pi}\left(\frac{\pi}{2}p+1\right)-\frac{m}{h}}{\frac{2}{\pi}\ln 2-\frac{2}{\pi}\ln 4+\frac{2}{\pi}\left(\frac{\pi}{2}p+1\right)}= \qquad \text{(II-4) cont.}$$

$$=\frac{0.2}{0.2+\frac{m}{h}}.$$

It will be seen from expression II-4 that the error is less than 4 per cent if $p = 5$, and less than 2 per cent if $p = 10$ and so on.

The foregoing analysis permits the following conclusions:

1. The face permeance is not reduced by more than 4 per cent by assuming that the field in the space between the teeth is uniform.

2. In calculating the permeance of the space between the recesses, the magnitude of this permeance is not increased by more than 4 per cent by assuming that the lines of force are along the line BB' (see Fig. 2) (i.e. assuming uniformity of the field between the teeth).

3. The total permeance (for the recesses and teeth), can be calculated to within 4 per cent since the error in calculating the two permeances differ in sign.

Appendix III. Suppose it is required to determine the permeance of a system with the following dimensions (tooth above tooth):

$$b=2\text{ cm};\ a=2c=1\text{ cm};\ \delta=0{,}1\text{ cm};$$
$$s=1\text{ cm};\ n=3.$$

The permeance G_1 is found from formula (1), in which k_{11} and k_{12} equal:

$$k_{11}=\sin\alpha_{11}=\frac{\delta}{b}=\frac{0{,}1}{2}=0.05\ (\alpha_{11}=3^\circ,\ \alpha'_{11}=87^\circ);$$

$$k_{12}=\sin\alpha_{12}=\frac{a}{b}=\frac{1}{2}=0.5\ (\alpha_{12}=30^\circ,\ \alpha'_{12}=60^\circ).$$

The values of the elliptic integrals are found tables:

$$K(k_{11})=1.57;\ K(k'_{11})=4.34;$$
$$K(k_{12})=1.68;\ K(k'_{12})=2.16.$$

Consequently,

$$\varphi(k_{11})=\frac{4.34}{1.57}=2.8;\quad \varphi(k_{12})=\frac{2.16}{1.68}=1.28.$$

Substituting the obtained and known data in formula (1), it is found that:

$$G_1=11.0\mu_0.$$

The permeance G_2 is found from formula (8). The relationship between the geometric dimensions of the system q = 4 provides the modulus k_{21}

$$k_{21}=\frac{4}{e^{2\pi}}=0.00735.$$

Consequently,

$$f=\frac{c}{k_{21}}=68\text{ cm};$$

It is also found that

$$\alpha_{21}=\arc\sin k_{21}=0°25';\ \alpha'_{21}=89°35';$$
$$K(k_{21})=1.57.$$

Using the calculated values of the partial elliptic integrals of the first kind (with $k'_{21}=\sin\alpha'_{21}$),

$$F(\varphi_{21};\ k'_{21})=K(k_{21})\,\frac{a-\delta}{c}=1.57\,\frac{0,9}{0.5}=2.82;$$
$$F(\varphi_{22};\ k'_{21})=K(k_{21})\,\frac{a+\delta}{c}=1.57\,\frac{1.1}{0.5}=3.45.$$

The amplitudes of the integrals from the tables are:

$$\varphi_{21}=83°30';\ \varphi_{22}=86°30'.$$

Finally, finding

$$k_{22}=\sqrt{\frac{f^2-(f^2-c^2)\sin^2\varphi_{22}}{f^2-(f^2-c^2)\sin^2\varphi_{21}}}=\sqrt{\frac{1-0.994}{1-0.988}}=0.71$$
$$(\text{i. e. } \alpha_{22}=45°;\ \alpha'_{22}=45°),$$

it is found from the tables:

$$K(k_{22})=K(k'_{22})=1.85.$$

Substituting these values in formula (8)

$$G_2 = 16.5\mu_0.$$

The permeance G_3 is found from formula (2a):

$$G_3 = \frac{v}{30}\left[\frac{2}{k_{11}} + 41\right]\mu_0 = \frac{2}{30}\left[\frac{2.2}{0.1} + 41\right]\mu_0 = 5.4\mu_0.$$

The total permeance of the system is equal to:

$$G = (11.0 + 16.5 + 5.4)\mu_0 = 32.9\ \mu_0.$$

The experimentally determined permeance equalled $G = 35\mu_0$. Thus, the error was of the order of 6 per cent which agrees well with the estimated error.

Translated by O.M. Blunn

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JOINT SELECTION OF THE MOTOR CRITERIA AND THE GEAR RATIO*

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Statement of the problem. When designing the method of starting an electric drive in which the rotational torque of the motor and the moment of resistance (load torque) of the driven device are constant, the following problems arise in connexion with the choice of the gear ratio:

1. Given the rotational torque M_m and the moment of inertia J_m of the motor and the load torque M_l and the moment of inertia J_d of the driven mechanism, it is necessary to determine that optimum gear ratio i_{opt} which will ensure the acceleration of the driven mechanism up to the desired speed ω_{do} in the shortest possible time [1,2,3].

2. Given the criteria M_l and J_d of the driven mechanism, it is necessary to determine that optimum gear ratio which will ensure uniform acceleration of the driven mechanism device to the desired speed ω_{do} in a specified time t_s with the minimum rotational torque and weight of the motor [4-8].

3. After ensuring that these conditions are fulfilled, it is necessary to determine the optimum gear ratio which will ensure the minimum gross weight of the motor, the supply unit (e.g. the motor-generator converter of the unit), and of the gears.

In all these cases it is assumed that the moment of inertia and weight of the motor are independent of its nominal speed and definable by the rotational torque of the motor M_m. This proposition follows from the fact that the permissible torque of a motor M_m under starting conditions depends mainly on the thermal capacity of the armature, since the heat transfer to the surrounding medium in the process of acceleration has no appreciable effect on the heating of the motor.

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The first problem, which has no great practical importance, arises when checking the conditions of acceleration of the driven mechanism and the correctness of the choice of gear ratio for a given motor.

In the second and third problems it is a question of selecting the gear ratio and the criteria of the motor together to ensure the most economical arrangement. These problems arise in designing all types of electric drive. The solution of the first problem is given by Umanskii's formula [1,2]

$$i_{\text{opt }1}=\frac{M_l}{M_m}+\sqrt{\left(\frac{M_l}{M_m}\right)^2+\frac{J_d}{J_m}}. \tag{1}$$

The problem of determining the optimum gear ratio for the minimum weight of the motor was first formulated by Geiler [4]. It has since been considered in a number of other papers [5-8]. However, no general solution is as yet available for any given type of relationship $J_m(M_m)$. The moment of inertia of the gears is usually ignored in the solution of this problem, and the efficiency of the gears η_g is either ignored [5, 6], or else only included approximately [8].

In his more recent paper [3] Geiler proceeds from Umanskii's formula for the solution of the second problem.

Substituting the value $i_{\text{opt }1}$ from (1) in the equation of motion of the drive, the author obtains the expression

$$M_{m.\min} = 2\,a\,i_{\text{opt}}J_m,$$

where

$$a=\frac{\omega_{d0}}{t_s}. \tag{2}$$

and t_s is the specified acceleration time.

Geiler argues that these expressions provide the solution for the practical problem of determining the optimum size of a motor which will ensure the acceleration of the driven device via the gears in the minimum (specified) time i.e. determined the necessary power of the motor taking both quantities M_m and $i_{opt\ 1}$ as variables at the same time.

In fact, however, the real position is quite different. In the first place it is not clear how $i_{opt\ 1}$ and M_m (all $i_{opt\ 1}$ and J_m) can be found from expression (2) at the same time, since two unknown quantities are contained in this expression. This aspect of the problem was not considered by Geiler [6].

Moreover, there is a problem of whether or not formula (2) can be used to select the gear ratio and the criteria of the motor simultaneously, since this formula, like Umanskii's formula, is derived on the assumption that M_m and J_m are given constant quantities which are independent of the gear ratio.

It is of course true on logical grounds that formula (2) must be valid whether the optimum criteria of the drive are found by changing the gear ratio alone, or by changing the gear ratio and criteria of the motor at the same. This logical consideration is however not sufficient to substantiate formula (2) if $M_m = f(i) = \text{var}$ and $J_m = f(i) = \text{var}$; it only follows that the stated formula can be proved more rigorously than in the paper in question.[6].

Other papers [5,7] give a solution of the second problem for the special case where use is made of a series of geometrically similar motors with a dynamo-electric constant. Under these conditions $J_m \equiv M_m^{5/3}$. Rozenberg [8] has considered another special case when use is made of motors with an identical ratio GD^2/M_m. However, the foregoing conditions as regards $J_m(M_m)$ often do not obtain in practice since this is uneconomic from the point of view of the construction of the motors and the utilization of expensive material.

The foregoing methods of determining $i_{opt\ 1}$ are not suitable for the most important practical case when there is a series of electric motors with different outputs (with different values of M_m) for which the values of J_m are known. In this case the relationship $J_m(M_m)$ is given graphically (plotted by points) and it is required to select the gear ratio and minimum weight of motor which will ensure the acceleration of the driven device in the specified time.

As regards the problem of selecting the optimum gear ratio which will ensure the minimum total weight of the drive motor, supply unit and gears, nothing has as yet been published on this question.

Criteria of the drive for the minimum weight of motor. For the joint selection of $i_{opt\ 1}$ and $M_{m.min}$, it is taken for granted that the relationship between the moment of inertia and the moment of the motor $J_m(M_m)$ is given graphically.

By way of example, Fig. 1 shows the relationship $J_m(M_m)$ for a series of small electric motors (curve 1) from which it will be seen that the moment of inertia of the stated series increases with increasing M_m to a degree slightly less than 5/3 (see curve 3 in Fig. 1).

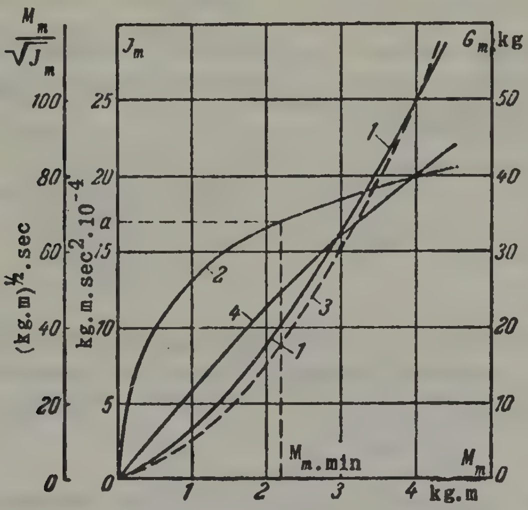

Fig. 1. Graphical determination of the gear ratio which corresponds to the minimum weight of the drive motor:

1 – Experimentally-determined relationship $J_m(M_m)$ for one of the series of motors; 2 – auxiliary curve $M_m/\sqrt{J_m}(M_m)$; 3 – relationship $J_m(M_m)$ for one series of motors for which $J_m \equiv M_m^{5/3}$ (based on a motor with M_m = 4 kg.m); 4 – weight of motor as a function of torque.

In order to determine the derivative $\frac{dM_m}{di}$, use is made of the equation of motion

$$(M_m i - J_m i^2 \alpha)\eta_g = M_b + J_g \alpha, \tag{3}$$

where $M_b = M_l + J_d\,\alpha$; J_g is the moment of inertia of the gears, in reference to the shaft of the drive of the driven device; and η_g is the efficiency of the gears.

Bearing in mind that the variables M_m, J_g and η_g are functions of i, and that J_d is a function of M_m, it is found after differentiating (3) that:

$$\frac{dM_m}{di}=\frac{(M_m-2\alpha iJ_m)\left(\eta_g+i\frac{d\eta_g}{di}\right)-\alpha\frac{dJ_g}{di}+\alpha i^2J_m\frac{d\eta_g}{di}}{i\left(\alpha i\frac{\partial J_m}{\partial M_m}-1\right)\eta_g} \tag{4}$$

Since the denominator on the right-hand side of formula (4) has finite values for finite values of α and i, it follows that the condition for the minimum of M_m takes the form

$$M_{m.\min}=2\alpha i_{\mathrm{opt}\,1}J_m-\alpha\frac{\left(\frac{dJ_g}{di}-i^2J_m\frac{d\eta_g}{di}\right)}{\left(\eta_g+i\frac{d\eta_g}{di}\right)} \tag{5}$$

It follows from formula (5) that equation (2) is only valid if J_g = const and η_g = const, independently of the shape of the function $J_m(M_m)$, whether or not M_m or J_m are constant, or whether they depend on i.

The quantities $M_{m.\min}$ and $i_{\mathrm{opt}\ 1}$ can be found by the graphical solution of eqn. (3) if this is converted into

$$M_m=\frac{M_b}{i\eta_g}+J_m\alpha i+\frac{J_g\alpha}{i\eta_g}. \tag{6}$$

Knowing the relationships $J_m(M_m)$, $\eta J_g(i)$ and $\eta_g(i)$, the right and left sides of expression (6) are plotted as a function of M_m on the assumption that the variable i is a known item. In this case the left-hand side φ_1 is a straight line at 45° to the base, and the right-hand side φ_2 is a series of curves corresponding to different values of i (Fig. 2).

The points of intersection of the curves φ_2 (M_m) and the line $\varphi_1(M_m)$ define values of M_m, which satisfy eqn. (6) for different values of i. It follows from Fig. 2 that with increasing i, the necessary moment of the motor M_m continuously decreases. The maximum value of i, at which it is possible to ensure the acceleration of the given device to the desired speed in the specified time, is defined by the co-ordinates of the point where the straight line φ_1 is tangential to the curve φ_2, which corresponds to $i = i_{\mathrm{opt}\ 1}$. At large values of the gear ratio, curves for φ_2 do not intersect the straight line φ_1 and condition (3) cannot be satisfied.

The optimum gear ratio $i_{opt\ 1}$ can be defined in this way for all values of M_l, including $M_l = 0$, provided $J_m \neq 0$; naturally, if $J_m = 0$ no optimum gear ratio is possible.

It can be shown that the product $M_m i$ which satisfies eqn. (6), continously increases with increasing i.

As stated above, in order to determine $i_{opt\ 1}$ it is necessary to know the relationships $J_g(i)$ and $\eta_g(i)$. These relationships can easily be established if the division of the total transmission ratio between individual stages and the criteria of adjacent stages is known.

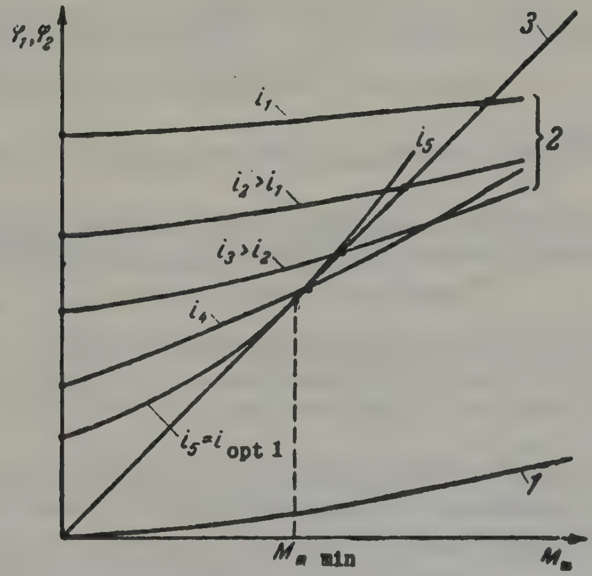

Fig. 2. Graphical solution of the equation of motion of the drive.

1 – relationship $J_m\alpha = f(M_m)$; 2 – curves $\varphi_2(M_m)$ for different values of i; 3 – straight line $\varphi_1(M_m) = M_m$.

If the moment of inertia of the gears is ignored, it is much easier to determine $M_{m.min}$ and $i_{op.1}$.

In this case eqn. (3) and (5) take the form:

$$\left.\begin{aligned} M_m i - J_m i \alpha &= \frac{M_b}{\eta_g}; \\ M_m &= 2\alpha i J_m. \end{aligned}\right\} \quad (7)$$

Given the efficiency of the gears and assuming that it is constant, it is possible to eliminate the variable i from the set of eqn. (7):

$$\frac{M_m}{\sqrt{J_m}} = 2\sqrt{\frac{\alpha}{\eta_g}(\alpha J_d + M_l)} = a. \quad (8)$$

This equation can be solved graphically as in Fig. 1. Knowing the relationship $J_m(M_m)$, it is also possible to plot the relationship $\frac{M_m}{\sqrt{J_m}} = f(M_m)$. First of all the value of a which is equal to the right-hand side of eqn. (8) is marked off along the base and using the curve $\frac{M_m}{\sqrt{J_m}}$ (curve 2) the motor torque $M_{m.\min}$ can then be found. Knowing $M_{m.\min}$ and J_m, the quantity $i_{\text{opt }1}$ can be found by formula (7). The value of η_g can then determined and compared with the value adopted previously.

If there is a large discrepancy between the new value of η_g and the old value, the calculations are repeated and no more difficulties arise owing to the simplicity of the constructions.

However, the optimum criteria of the drive which are obtained in this way are not always practicable because extremely high rated rotational speeds of the motor may be obtained.

It should also be stressed that the stated method of selecting the criteria of the electric drive from the point of view of economy is not correct. Increases in the gear ratio increase the difficulties of production and reduce the efficiency; a high gear ratio involves an increase in power and weight of the supply unit.

In fact, the electromagnetic power of a generator at the end of the acceleration of the driven mechanism equals

$$P_g = E_{g0} I_a = \frac{\omega_{m0} M_m}{1 - s} = \frac{i\omega_{d0} M_m}{1 - s}, \quad (9)$$

where $s = \frac{I_a r_a}{E_{g0}}$, r_a the total resistance in the circuit of the motor armature and generator, I_a the armature current, E_{g0} the e.m.f. of the generator at the end of acceleration.

Substituting the value $M_m i$ from (3) in (9),

$$P_g = \frac{\omega_{d0}}{1-s}\left(\frac{M_b}{\eta_g} + a i^2 J_m\right). \tag{10}$$

An increase in the transmission of the gears is accompanied with a continuous increase in the power of the generator and the weight of the supply unit, since the second term on the right-hand side of formula (10) increases continuously in this case.

It can be shown that the total expenditure of energy by the generator in the process of accelerating the given mechanism with losses included for the main circuit of the generator and motor (if I_a = const, M_l = const, and M_m = const) equals:

$$A_{tot} = \frac{\omega_{d0}^2}{2}\left(J_d + i^2 J_m\right)\frac{1+s}{1-s} + \frac{M_l \omega_{d0} t_s}{1-s}. \tag{11}$$

For the optimum gear ratio selected in accordance with the method expounded above, the power of the generator and the total expenditure of energy by the generator will be maximum

$$P_{g1} = \frac{2\omega_{d0} M_b}{1-s}; \tag{12}$$

$$A_{tot1} = \frac{\omega_{d0}^2}{1-s}\left[J_d(1+s) + \frac{M_l}{a}\right]. \tag{13}$$

Consequently, it is not economical to chose the gear ratio merely to obtain the minimum weight of the drive motor.

Minimum gross weight of the motor, supply unit and gears. It is advisable to select that value $i = i_{opt\ 2}$ at which the gross weight of the motor supply unit and gears is minimum because the weight of the motors decreases with increasing transmission, but the weight of the supply unit and gears decreases with increasing gear ratio:

$$G_\Sigma = G_m + G_s + G_g, \tag{14}$$

where G_m is the weight of the motor, G_s is the weight of the supply unit, and G_g is the weight of the gears.

This problem can be solved graphically if the relationships $J_m(M_m)$, $G_m(M_m)$, $G_s(P_g)$ and $G_g(i)$ are known. These relationships can be defined analytically or graphically. As stated above, the weight of the motor is proportional to the rotational torque M_m. The weight of the supply unit depends upon the type of motor for the generator. In selecting the supply unit from its electromagnetic overload capacity, its weight is fixed as a function of the electromagnetic power of the generator.

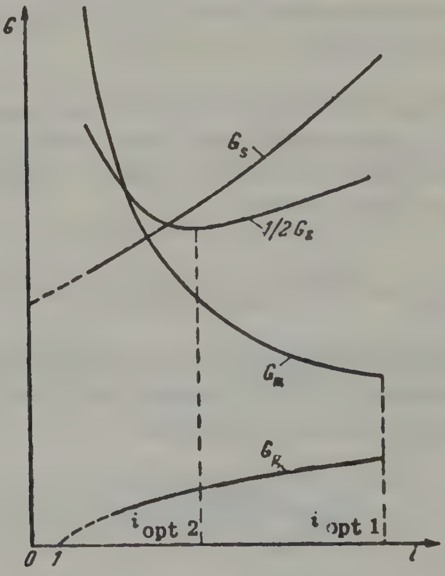

Fig. 3. Weight of drive motor G_m, supply unit G_s and gears G_g and the gross weight G_Σ as a function of the gear ratio.

In order to solve this problem, it is first necessary to determine the quantities M_m and P_g as a function of the gear ratio, and then to determine the weight G_m, G_s and G_g and the gross weight as a function of i.

To do this, use is made of the results of graphical integration of the equation of motion of the electric drive in Fig. 2. The points of intersection of the curves $\varphi_2(M_m)$ and the straight line φ_1 corresponds to definite values of M_m which satisfy the equation of motion. Using this data it is possible to plot the relationship $J_m(i)$ direct and then determine the weight of the motor as a function of i.

Substituting the values thus found for M_m and i in formula (11), the power of the generator is determined as a function of i. Knowing the relationship between the weight of the supply unit and P_g, it is then possible to plot $G_s(i)$.

The weight of the gears is the sum of the weight of the gearwheels and the housing. Assuming that the latter is proportional to the weight of the gearwheels, it is possible to plot the weight of the gears as a function of i.

Plotting the stated relationships $G_m(i)$, $G_s(i)$ and $G_g(i)$ on a common diagram (Fig. 3), the gross weight can then be plotted as a function of i. The required optimum value of the gear ratio $i=i_{\text{opt }2}$ corresponds to the minimum gross weight. It will be seen from Fig. 3 that the optimum gear ratio $i_{\text{opt }2}$ is considerably less than the gear ratio $i_{\text{opt }1}$ for the minimum weight of the drive motor alone. The difference between $i_{\text{opt }2}$ and $i_{\text{opt }1}$ will be the greater, the greater the weight G_s and G_g compared with the weight G_m.

Conclusions

1. For the joint selection of the gear ratio and the criteria of the drive motor it is possible to minimize the weight of the drive motor alone, or to minimize the gross weight of the motor, supply unit and gears altogether. Either problem can be solved graphically if the relationships $J_m(M_m)$, $G_m(M_m)$ and $G_s(P_g)$ are known. These relationships can be defined graphically in the general case or analytically.

2. It is not economical to select the criteria of a drive having an individual supply source solely from the point of view of minimizing the weight of the drive motor as this leads to unduly high gear ratios, and excessive power and weight of the supply unit.

In selecting the gear ratio and criteria of the drive motor, the aim should be to minimize the gross weight of the motor, supply unit and gears. This considerably reduces the transmission ratio and provides a much more efficient system. This approach is justified on economic grounds.

Translated by O.M. Blunn

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ABSTRACTS FROM PAPERS PUBLISHED IN ELEKTRICHESTVO No.7, 1961

Leading article

To new achievements in the development of Soviet science. (pp. 1-6).

An account is given of a recent conference which was held in the Kremlin to discuss the future development of Soviet science. Development work, applied research and fundamental research was discussed.

Capacitors

Determining the initial conditions for sudden changes in voltages and currents. I.A. Zaitsev *et al.*, (pp. 52-55).

A contribution is made to the determination of initial conditions when sudden changes occur in the currents in inductances or voltages across capacitors. Simple schemes are obtained by analysing commutation behaviour which make it possible to determine the initial conditions for sudden changes in voltages and currents quickly.

Determining the reactive power in capacitors. N.I. Nazarov *et al.* (55-59).

It is considered that the specific dissipating surface of a capacitor varies with the shape of the housing and its dimensions. For a given volume, the dissipating surface is maximum for a rectangular parallelepiped with the minimum permissible width of base.

Control engineering

Use of the phase plane method for studying transient mechanical behaviour in electric drives. L.I. Gandzha, (pp. 68-72).

Phase diagrams of transient behaviour are constructed from an analysis of the equation of motion of the drive for the various possible types of load.

The use of saturable reactors to prevent variation in the torque of induction motors due to transient currents. M.M. Sokolov, (pp. 72-75).

An account is given of tests on electric drives with induction motors and saturable reactors which show that a smooth increase in motor torque and speed can be obtained on starting and reversing. The connexion of saturable reactors in the stator circuit only increases acceleration time slightly. Acceleration time depends on the magnetization current of the reactor.

A frequency-controlled drive for a horizontal planing machine. D.A. Zavalishin *et al.*, (pp. 75-79).

It is argued that the best frequency-controlled asynchronous drive system for horizontal planing machines is one with a compensated collector generator excited on the stator side. A detailed account is given of a prototype system.

Elimination of zones of insensitivity in ferromagnetic pick-ups and amplifiers. B.K. Karpenko, (pp. 84-86).

A cheap and effective way of eliminating dead bands in pick-ups and amplifiers with ferromagnetic cores is proposed by the use of an additional magnetic circuit.

Power systems

Zero sequence equivalent networks for transmission lines with different voltages. N.G. Geinin, (pp. 79-81).

It is shown how to compile a zero sequence equivalent network which includes the mutual inductance between two or several transmission lines when each of the nearby lines is of a different voltage.

Rotating machines

The effect of excitation systems on the stability of large interconnected turbo-generators. V.M. Bobrov *et al.*, (pp. 7-13).

A study is made of the stability of large turbo-generators with different systems of excitation. The use of electrodynamic modelling has made it possible to use natural excitation regulators, find their optimum adjustment and study electromechanical phenomena during transient behaviour.

Artificial damping in large synchronous machines.

I.D. Urusov *et al.*, (pp. 13-18).

Instead of increasing the flywheel mass, it is proposed to use a system of artificial damping to reduce the oscillation of synchronous machines with a pulsating load. Artificial damping is based on the creation of an additional electromagnetic rotational torque on the shaft by introducing additional periodic currents in the rotor circuits. The theory of the method is given along with test results.

Analysis of dynamic stability with the effect of damper windings, speed governors and excitation included.

K.I. Bogatev, (pp. 31-34).

A refinement to the method of successive intervals is proposed which makes it possible to include the effect of damper windings, speed governors and excitation when an analytical estimate of dynamic stability is required for synchronous alternators.

Experimental determination of the m.m.f. of the armature commutation reaction in d.c. machines. V.A. Lifanov, (pp. 81-84).

A method is proposed which dispenses with calculated data in the determination of the m.m.f. of the armature commutation reaction by a Hall e.m.f. transducer.

Traction

Reduction of wheel-spin on electric locomotives.

B.P. Petrov, (pp. 35-41).

A study is made of the factors affecting high adhesion factors for electric locomotives. Recommendations are made for automatically stopping wheel spin.

Transformers

Problems of voltage regulation in auto-transformers.

A.G. Kraiz, (pp. 41-48).

A method is described for comparing different systems of voltage regulation for large auto-transformers using the typical power of auto-transformers. A refined method of analysis is proposed for the system of regulation in the neutral of the auto-transformer. It can be used to specify the limits of regulation with over- or under-excitation. A short description is given of a new 220 kV auto-transformer with built-in apparatus for regulating the voltage.

Voltage stabilizers

Ferroresonant voltage stabilizers using magnetic material with a rectangular characteristic. D.I. Bogdanov, (pp. 48-51).

A theoretical study is made of the phenomena associated with the operation under load of ferroresonant voltage stabilizers in which the saturable core is made of cold-rolled steel or magnetic alloy with a high coefficient of rectangularity.

THE PROBLEMS OF DEVELOPING NEW SMALL AND F.H.P. MOTORS*

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The development of industry, transport, agriculture and the increasing standards of living have considerably extended the demand for small and f.h.p., d.c. and a.c. motors.

The purposes which such equipment has to serve are extremely diverse, covering automatic control, traction, cable and wireless, instrumentation, agriculture and domestric appliances such as clocks, cinematograph equipment etc.

The production of small and f.h.p. motors is now a special branch of electrical engineering whether or not the motors are for special purposes or of general design. The increasing output of various kinds of motor for industry and consumption goods is increasingly demanding more and more material. Whereas, for example, the production of large machines requires about 2 to 3 kg of active and constructional material per 1 kW, small and f.h.p. motors require on average about 50 g per 1 W.

In order to use electrical energy efficiently and economize in ferrous and non-ferrous metal, special attention has to be paid to the starting and operating characteristics of the motors and the utilization of material.

In this paper an attempt is made to classify the problems which have to be solved in this field into those which are common to all classes of machine up to several hundred watt, and those which refer specially to particular types.

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* *Elektrichestvo*, 8, 4-7, 1961.

Amongst the general problems, we have:

(a) the development of new types of d.c. and a.c. motor and the improvement of existing models;

(b) the reduction in size and weight without affecting heating conditions and starting and operating characteristics;

(c) the reduction in mechanical inertia of the executive a.c. and d.c. control motors for small servo systems and reversible equipment;

(d) the use of new magnetic materials and alloys, winding conductors, heat-resistant insulation, varnishes and lubricants in order to improve efficiency and reliability and develop small machines capable of operating at high temperatures;

(e) the improvement in production technology, including the automation of stamping, winding, assembly and other time-consuming operations;

(f) the review of existing State Standards and the development of new ones for individual classes of machine and test methods.

It is well known that the design of small and f.h.p. motors is very diverse and that there is no standard method of connecting the motor to the executive device or instrument. Quite often the motor is organically connected with the load device so that the motor no longer retains its original design features. This often occurs in special equipment and domestic appliances. On the other hand, the design of the motor does not always satisfy requirements and needs to be brought up to date.

Furthermore, there is an increasing demand in all fields of application for motors of essentially new design with improved characteristics as regards weight, size, mechanical inertia and efficiency etc. A good illustration is the small d.c. motor with a printed armature winding. But there are also other opportunities for improvement based on new materials, combined winding circuits, modified magnetic systems etc.

Perhaps the most important problem in this field is the reduction in size and weight. As we have seen, the specific consumption of active and constructional material per unit of output is roughly 20 times greater in small and f.h.p. machines than in large machines. A reduction in weight would therefore result in a considerable national saving in ferrous and non-ferrous metal.

The weight of a machine can be reduced by reducing its size, and this in turn can be done by altering its construction, improving the ventilation and using new magnetic and constructional material. But, as is known, the size of a machine affects its starting and operating characteristics and its heating capacity. Yet the heating capacity of a model can also depend considerably on the conditions of heat transfer at the point where it is installed. In this respect the weight of the motor is linked mainly with the method of securing the housing of the motor to the load equipment, the type of material used for this gear and its surface area for heat transfer. Unlike medium and large electrical machines, this is a very important factor as regards the heating of small and f.h.p. motors.

At the present time a variety of different fixtures are used to secure small and f.h.p. motors to the test stands, namely, special metal "squares", columns with clamps, and other rigs mounted on metal plates.

Test experience shows, for instance, that if a motor is secured by flanges to metal squares, the heating of the motor will vary with the surface area of these squares even with a constant load. Before standard test conditions can be established, it is first necessary to find the best fixtures for securing the test machines. In this respect the important problem is to determine in what limits the ratio of the surface area of the test fixture to that of the machine can be left to vary safely.

The solution of this problem would make the thermal analysis of a number of small non-ventilated electrical machines more exact in the project stage and would help to minimize their size for given outputs and speeds. This is one of the most effective ways of reducing the weight of the small machine.

The problem of reducing the mechanical inertia of small control motors is also an important one from the point of view of improving the the accuracy of small servo systems and reversible gear in performance.

It is well known that accurate performance from high-speed automatic systems depends to a considerable extent on the mechanical inertia of the rotor of the executive motor and that of the other rotating elements in the system. The less the electromechanical time constant of such systems, the more accurate is the performance and the faster the response. Since the rotor moment of inertia of a small motor is proportional to the product of the fourth power of its diameter and the first power of the packet length (i.e. the length of the stack of stampings), it follows that the best method is to reduce the diameter as far as possible and accordingly lengthen the rotor to retain the same volume.

In order to improve the efficiency and reliability of small special electrical machines, extensive use should be made of new magnetic materials, alloys, heat-resistant winding conductors, insulating material, varnishes, enamels, bearing lubricants and wiring systems.

This is specially important for small heat-resistant motors capable of operating reliably at temperatures of up to 250°C. However, existing winding conductors and insulation do not as yet satisfy these requirements sufficiently and the maximum operating temperature is still at 180°C. The position is the same with the other material listed above.

It would be of great value to pay more attention to mechanization and automation of stamping, winding, assembly and other time-consuming operations in order to reduce the cost and increase the output of small and f.h.p. machines. Insufficient attention is at present being paid to these matters in industry and "special organizations" engaged on the production of these machines.

Finally, another general problem is the revision of existing State Standards and the development of new ones for particular classes of machine and test methods. The existing standards in the U.S.S.R. only cover certain types of machine, e.g. GOST 6435-52 "Electrical motors of 5 to 600 W", GOST 8212-56 "Three-phase asynchronous motors from 10 to 600 W", GOST 6117-54 "Electrical d.c. generators for automobiles" , and so on. These standards naturally refer to the level of technique as it was at the date of publication, and it is important to keep them up to date and publish new ones as required. Special importance attaches to the question of test methods, and the details involved in the experimental determination of motor parameters.

Besides the enumerated general problems, there are a large number of particular problems in motors for special purposes. Here the most pressing are:

(a) the improvement of commutation in small d.c. generators and motors under reversing conditions with a reversing frequency of up to 5 to 10 c/s;

(b) greater accuracy in the performance and output criteria of rotating transformers, selsyns and tachogenerators;

(c) the refinement of design methods for small machines with permanent magnets, hysteresis motors and various reactive motors;

(d) the development of bearings for sustained operation without additional lubrication;

(e) the abatement of noise;

(f) the improvement of motors for domestic appliances.

The solution of each of these problems would be of great value in improving the quality of small special purpose machines.

Thus, a need to improve the commutation of small d.c. generators and motors arises when they are operating as motors and generators in certain special purpose plants. In such plants an executive motor usually has to operate with a relatively large flywheel mass on the shaft under reversing conditions with a reversing frequency of up to 5 to 10 c/s. Under these conditions the motor and generator are practically continually affected by transient phenomena so that current commutation is very seriously affected. Appropriate means have still to be developed to improve the commutation, since the use of compoles for this purpose in small machines is made difficult by the lack of space to accommodate them.

In order to improve the operational accuracy of computer systems, radar scanning systems, small servo systems and other automatic devices, it is necessary to improve the output parameters of rotating transformers, selsyns and a.c. and d.c. tachogenerators which are widely used in the stated equipment. Althoug modern sine-cosine rotating transformers very accurately reproduce a sinusoidal relationship between the output voltage and angle of rotation of the rotor, they are still not always adequate for the increasing demands which are continually being made upon them.

As regards the design of small machines with permanent magnets, hysteresis motors and various reactive motors, it should be pointed out that the existing analytical methods are largely approximate and empirical and the results which they produce often diverge notably from test data. Much theoretical and experimental work needs to be done to refine these methods.

One of the most important constructional components in small high-speed motors for gyroscope and grinding (polishing) equipment etc. is the bearings which to a large extent determine the reliability of these motors. Since the speed of motors can reach several tens of thousands of revolutions per minute, new types of bearing need to be developed as conventional bearings are inadequate for the purpose.

As regards motors for domestic appliances, the problem is to find the most efficient type of motor for each particular appliance and improve its performance, reliability and life.

In conclusion it should be pointed out that the solution of these general and special problems depends on the efforts of many specialists, industrial enterprises, design offices, research organizations, and educational establishments engaged on the design, production, testing and use of these machines and everything should be done to extend research and development in this field and make the results as widely known as possible.

Translated by O.M. Blunn

THE MEASUREMENT OF LARGE TRANSIENT CURRENTS FOR SWITCHGEAR*

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In short-circuit current tests on switchgear it is necessary to measure large transient currents which sometimes reach several hundred thousand amperes. Shunts or current transformers and electromagnetic oscillograph vibrators are usually used for this purpose.

This position is not satisfactory for on the one hand the test current may contain a constant component and the current transformers will considerably distort the secondary current curve owing to the intense magnetization of the steel. However, tests in the switchgear laboratory of the Lenin Electrical Engineering Institute have shown that satisfactory measurements can be taken by current transformers at double and treble overloads. On the other hand, shunts have the disadvantage that an involuntary delay occurs at the point where the shunt is connected. It is therefore desirable to find a method of measurement which combines the best features of shunts and current transformers. Such a method has now been evolved by using air-cored current transformers.

An air-cored current transformer is a toroidal coil through which a live conductor is passed. At the ends of the winding an e.m.f. is induced which is proportional to the current derivative. If this e.m.f. is integrated, a voltage is obtained which is proportional to the current to be measured. Integration is performed by an *RC* circuit in the proposed method. The necessary resistance *R* is possessed by the winding itself of the air-cored transformer coil and it is therefore made from a conductor with a large resistance. This construction of the coil has the advantage that the voltage is uniformly distributed over the turns on load. This simplifies the insulation of the connexions and considerably widens the range of currents that can be measured by one coil. The integration will be more accurate, the greater the integration constant $T = RC$ and the smaller the current in the toroidal winding. The magnitude of R is therefore made large and that of the voltage on the integrating capacitance small. The capacitor voltage is measured by a special d.c. valve amplifier.

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* *Elektrichestvo,* 8, 46-50, 1961.

Thus, three elements are required for the proposed system:

1. a toroidal coil with a high impedance winding;

2. a d.c. amplifier with an integrating capacitance at the input;

3. an electromagnetic oscillograph vibrator.

For sinusoidal current, the amplitude of the e.m.f. at the ends of the toroidal coil is:

$$U_T = M\omega I_m,$$

where M is the mutual inductance of the toroidal winding and live conductor, mH, ω the angular frequency of the current, and I_m the amplitude of the current to be measured.

The voltage at the input to the amplifier after integration is found quite accurately at large values of T from the expression

$$U_C = \frac{1}{\omega T} U_T.$$

If the gain of the amplifier is S_a, then the current at the output of the amplifier is

$$i_2 = S_a \frac{M}{T} I_m.$$

The quantity $S_a\ M/T = k_T$ is the transformation coefficient of the measuring circuit; if I is measured in kA, M in mH, T in sec and S_a in mA per volt, then i_2 is obtained in mA.

The error in measurement. We will now consider the error in measurements of the current by an air-cored current transformer. The degree of accuracy will depend on the accuracy with which the current derivative is obtained, the accuracy of integration and the performance of the amplifier (linearity of characteristics for d.c. and a.c.).

The first and third conditions are satisfied if the toroidal coil and amplifier are right. Good characteristics (1 per cent accuracy in measurement) can be obtained for this equipment without any special difficulty under laboratory conditions. A description of the Institute's toroid and amplifier will be given below.

The second condition can be satisfied within definite limits (tolerances). The equivalent circuit of an air-cored current transformer is shown in Fig. 1.

The differential equation in the capacitor voltage U_c will be:

$$LC\frac{d^2u_C}{dt^2}+RC\frac{du_C}{dt}+u_C=M\frac{di}{dt}. \qquad (1)$$

The charge current i_2 of the capacitance C is neglected in comparison with the magnitude of the magnetization current i_1. The short-circuit current may contain periodic and aperiodic components. The transformation of these components will be considered separately.

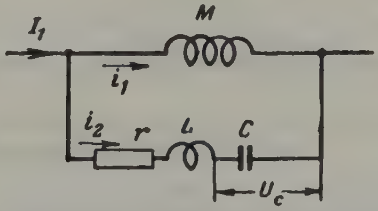

Fig. 1. Equivalent circuit of air-cored transformer.

The *symmetrical component* of the test current varies according to the law

$$i=I_m\sin(\omega t+\psi). \qquad (2)$$

Then

$$\frac{di}{dt}=MI_m\omega\cos(\omega t+\varphi). \qquad (3)$$

When considered with (3), the solution of eqn. (1) will be

$$\left.\begin{array}{c} U_C = \frac{M}{T_C} I_m \cos\varphi \Big\{ \sin(\omega t + \psi - \varphi) - \\ \frac{1}{k_1 - k_2} [(-k_2 \sin(\psi - \varphi) + \\ + \omega \cos(\psi - \varphi) e^{k_1 t} + (k_1 \sin(\psi - \varphi) - \\ - \omega \cos(\psi - \varphi)] e^{k_2 t}) \Big\}, \end{array}\right\} \quad (4)$$

where

$$k_1 k_2 = -\frac{r}{2L} \pm \sqrt{\frac{r^2}{4L^2} - \frac{1}{LC}};$$

$$\varphi = \text{arc tan} \frac{\omega L - \frac{1}{\omega C}}{\sqrt{r^2 + \left(\omega L - \frac{1}{\omega C}\right)^2}}.$$

Suppose we put

$$\frac{2L}{r} = T_L$$

and

$$RC = T_C,$$

then at the low values of L and φ which must be maintained in the tests,

$$k_1 = \frac{1}{T_C};$$

$$k_2 = -\frac{2}{T_L}.$$

Accordingly, eqn. (4) can be transformed into

$$\begin{array}{c} U_C = \frac{M}{T_C} I_m \cos\varphi \Big\{ \sin(\omega t + \psi - \varphi) - \\ - \sin(\psi - \varphi) e^{-\frac{t}{T_C}} - \frac{\omega L}{r} \cos(\psi - \varphi) \times \\ \times \left(e^{-\frac{t}{T_L}} - e^{-\frac{t}{T_C}} \right) \Big\}. \end{array} \quad (5)$$

Comparing (5) and (2), expressions can be obtained for the error in transformation of sinusoidal current:

1. In the steady state:
the amplitude error is

$$\beta_{st} = (1 - \cos\varphi)\,100\%; \tag{6}$$

and the angular error

$$\varphi \approx \frac{\omega T_L}{2} - \frac{1}{\omega T_C} \quad [\text{rad}].$$

2. Under transient conditions:
the amplitude error is

$$\beta_{tr} \approx \mp \cos\varphi\left(\omega T_L - \frac{1}{\omega T_C}\right)100\% \approx \approx \mp\left(\omega T_L - \frac{1}{\omega T_C}\right)100\%. \tag{7}$$

and the angular error

$$\varphi_{tr} \approx -\varphi \mp \beta_{tr}$$

or

$$\varphi_1 \approx -\frac{3}{2}\,\omega T_L + \frac{2}{\omega T_C} \quad [rad];$$

$$\varphi_2 \approx -\frac{\omega T_L}{2} \quad [rad]. \tag{8}$$

The minus sign and subscript 1 refer to odd half-periods, and the plus sign and subscript 2 to even half-periods.

The following assumptions were made in this analysis:

$$T_L \ll T_C; \;\; \sin\varphi \approx \tan\varphi \approx \varphi, \;\; \cos\varphi \approx 1.$$

Thus, in this case the error in measurement depends on the frequency ω, of the current, the inductance of the current transformer and the integration time constant $T_C = RC$.

The *aperiodic component* of the short-circuit current is expressed by the equation

$$i_a = I_{ma}\, e^{-\frac{t}{T_a}}. \tag{9}$$

In considering the error in transformation of the aperiodic current it is not possible to include the inductance L of the toroidal winding since $\omega \to 0$ and as a rule $\frac{L}{r} \ll rC$. Eqn. (1) then takes the form

$$T_C \frac{du_C}{dt} + u_C = -\frac{M}{T_a} I_{ma} e^{-\frac{t}{T_a}}. \tag{10}$$

The solution in U_c will be

$$U_{Ca} = \frac{M}{T_C} I_{ma} \frac{T_C}{T_C - T_a} \left(e^{-\frac{t}{T_a}} - \frac{T_a}{T_C} e^{-\frac{t}{T_L}} \right). \tag{11}$$

It follows from expression (11) that the aperiodic component of the capacitor voltage U_{ca} will decay more quickly than in the primary current and may even change sign. The error in measurement will depend on the ratio $\delta = T_C/T_a$, i.e. on the time constant of the measuring circuit and the attenuation time constant of the aperiodic component.

The error in measurement will be maximum at the instant the capacitor voltage U_C passes through zero. This instant is defined by the time

$$t_0 = T_a \frac{\delta}{\delta - 1} \ln \delta. \tag{12}$$

The maximum error in the measurement of the aperiodic component is found from the expression

$$\gamma = \delta^{-\frac{\delta}{\delta - 1}}. \tag{13}$$

The conclusion therefore can be drawn that the error in measuring by an air-cored transformer will depend on the frequency of the current to be measured. The size of the error at high frequencies will largely be determined by the inductance time constant of the secondary circuit $T_L = \frac{2L}{r}$, and at low frequencies (or aperiodic currents) by the capacitance time constant of the secondary circuit

$$T_C = RC.$$

Given 5 per cent permissible error in the transformation of aperiodic currents which decay with a time constant $T_a = 0.05$ sec, it is found from expression (13) that $T_C = RC = 1$ sec. In practice this condition is satisfied and T_L is about 1 to 2 μ sec. In this case an amplitude error of 5 per cent for sinusoidal currents from expression (7) will be at a frequency of the order of 4 kc/s. For the fundamental harmonic 50 c/s this error will be about 80 times less.

It therefore follows that an air-cored current transformer is suitable for measuring non-sinusoidal currents which combine in a series of the type

$$i = I_1 \sin(\omega t + \varphi_1) + I_3 \sin(3\omega t + \varphi_3) + \\ + I_5 \sin(5\omega t + \varphi_5) + \ldots$$

However, the angular error increases with increasing frequency of the measured current. Ignoring the effect of $\frac{2}{\omega T_C}$ in the expression for φ_1 in (8), the angular error can be calculated by the formula

$$\varphi_1 = \frac{3}{2} \omega T_L$$

or, if φ_1 is expressed in degrees, the permissible time constant T_L can be calculated from the angular error by the formula

$$T_L = \frac{\varphi_p^\circ}{3 \cdot 180 \cdot f},$$

where φ_p^o is the permissible angular error and f the frequency of the current c/s.

Thus, for a test current at 1000 c/s with 5 el degrees permissible angular distortion of the measurements, the time constant T_L must equal 9 μ sec.

Design of the air-cored current transformer. The air-cored current transformer must satisfy two main requirements, namely, it must be insensitive to external magnetic fields and changes in position of the current conductor inside the transformer coil. Since the magnetic permeance of the main flux and the leakage fields are the same in air-cored coils, it therefore follows that these conditions will be satisfied by an ideal toroid with an infinitesimal turn area and an infinitely large number of turns.

According to the law of total current, the following equation will hold for such a toroid

$$\oint H dl = 0,$$

if there are no live conductors inside the toroid. But if there are such conductors inside the toroid, then

$$\oint H dl = \Sigma I,$$

where H is the vector of field intensity and l the length of the closed contour (toroid).

The flux over the internal section of the toroid for a toroidal coil of external radius R_{ext} and internal radius R_{int} with w_2 rectangular turns of height h is

$$\Phi = \frac{\mu_0}{2\pi} I_M w_1 h \ln \frac{R_{ext}}{R_{int}}$$

The mutual inductance of the toroid with the primary winding will be defined by the expression

$$M = \frac{\psi}{I_M} = \frac{\mu_0}{2\pi} w_1 w_2 h \ln \frac{R_{ext}}{R_{int}} [\mathrm{H}].$$

The mutual inductance M in mH, referred to one turn of the primary current, is the main characteristic of a toroid. Another important characteristic affecting the accuracy of the measurements in the integration of the derivative is the inductance of the toroid:

$$L = \frac{\mu_0}{2\pi} w_2^2 h \ln \frac{R_{ext}}{R_{int}} [\mathrm{H}].$$

The inductance L increases the degree of error and it must therefore be small.

The toroid can be made practically insensitive to external magnetic fields if the width of the turn is less than 0.1 of the mean radius and if there are not more than 30 turns per 1 cm of the mean length of the circumference. The winding must be of two-layer design to obtain a large e.m.f.

The Institute's workshop has turned out air-cored transformers with the following rating:

Rated voltage – 12 kV.

Dimension of bakelite former for winding toroid: $R_{int} = 120$ mm, $R_{ext} = 124$ mm, $h = 178$ mm.

Number of winding layers – 2 (continuous, compact).

Winding conductor – enamelled nichrome, 0.15 mm.

Total resistance of winding – $R = 160\ \mathrm{k}\Omega$.

Mutual inductance – $M = 19.5\ \mu\mathrm{H}$.

Inductance of toroid – $L = 150$ mH.

Time constant – $T_L = 2\ \mu\mathrm{sec}$.

The method of winding the toroid is illustrated in Fig. 2(a). A photograph of the transformer is given in Fig. 3.

In order to compensate the effect of external transverse fields, a reverse turn of sheet aluminium foil is continued at the beginning of the winding of each layer. The internal layer of foil also serves as an electrostatic screen between the live conductor and the winding. The transformer is covered by a dural shell.

Tests on this toroid have shown that an e.m.f. of 6.25 V is induced at the ends of the winding for a conductor carrying a current of 1 kA at 50 c/s; changes in the position of the live conductor in the coil did not alter the result.

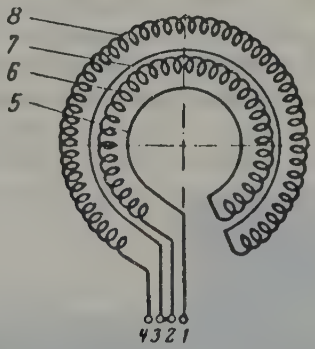

Fig. 2. Winding system of current transformer: *1* – beginning of first layer of foil; *2* – end of first layer of winding; *3* – beginning of second layer of foil; *4* – end of second layer of winding; *5* – first layer of aluminium foil; *6* – first layer of winding; *7* – second layer of aluminium foil; *8*-second layer of winding.

When a live conductor was placed outside the coil, the maximum registered e.m.f. (for various positions and distances) was 35 mV/kA, i.e. less than 0.5 per cent of the measured quantity.

The connexion of such a toroid to a capacitance of 6 μF provides integration of the induced e.m.f. with a time constant

$$T_C = RC = 160 \cdot 10^3 \cdot 6 \cdot 10^{-6} = 0.96 \text{ sec.}$$

In accordance with the above formulae the error in measurement will be within 5 per cent for the symmetrical component at a frequency above 4 kc/s and for an aperiodic component decaying with a time constant less than 0.05 sec.

In order to take oscillograph recordings of the transient current, use should be made of a d.c. amplifier with a gain S_a = 130 mA/V, which ensures a current in the vibrator circuit with a primary current of 1 kA

$$i_2 = 130 \frac{19.5 \cdot 10^{-6}}{0.96} = 2.25 \text{ mA.}$$

This makes it possible to take satisfactory recordings at currents over 10 kA.

Owing to the use of a high resistance conductor for the toroid, the upper limit to the currents which can be measured is set by the current capacity of the main bus in the "window" of the transformer. The transformer has been used to measure transient currents up to 200 kA.

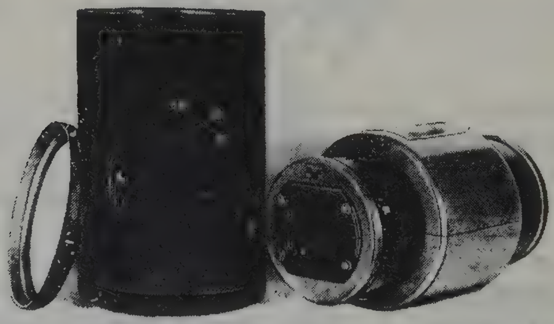

Fig. 3. Air-cored current transformer with casing.

Fig. 4 shows an oscillogram of a current of 116 kA in dynamic stability tests on a circuit breaker. The oscillogram was taken by means of an air-cored current transformer. For purposes of comparison, the same diagram also shows an oscillogram of the current obtained from a conventional 5000/5 A current transformer on the high-voltage side of the 6.6/0.25 kV supply transformer, where the current during the test amounted to 116 x 0.25/6.6 = 4.8 kA, i.e. kept within nominal limits. Comparison of the curves on the primary and secondary side shows that the measurements were identical as regards both the symmetrical and aperiodic components of the test current. In order to confirm the anticipated degree of error in the measurements, tests were carried out with an air-cored current transformer together with an a.c. shunt, but no difference was discovered in the recordings outside the limits of accuracy of the oscillograph.

Amplifier. In order that the constant component of the current should be transmitted with the minimum distortion for measuring the voltage on the integrating capacitance, use was made of a two-stage d.c. balanced amplifier with a low-impedance output.

A 6N9-type double triode with current stabilization was used in the first stage. Current stabilization was provided by a saturated pentode with automatic bias. The anode supply came from four stabilizers with a voltage +210-255 V relative to the body. The maximum amplification factor of the first stage was 40.

A 6N5-type double triode was used in the second stage (the power amplifier) also on the balanced system with cathode load. The anode supply was from a separate rectifier. For a direct link with the first stage, use was made of a voltage "tributary" of + 105 V from the stabilivolts of the anode rectifier in the first stage.

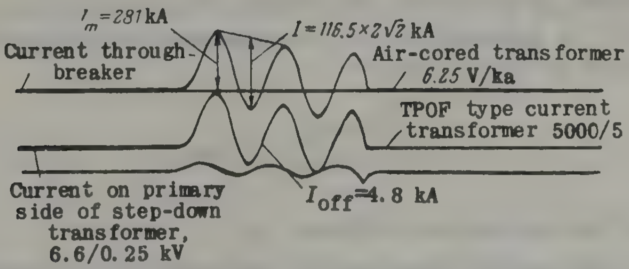

Fig. 4. Current oscillogram obtained by air-cored current transformer.

The characteristic of the d.c. amplifier was linear at output currents between 0 and 65 mA, i.e. the maximum input voltage at maximum amplification must not be above 0.5 V. If the amplification is reduced by a factor of 10, the input voltage must be increased to 5 V. In order to linearize the frequency characteristic of the amplifier use was made of a system of capacitance correction using negative feedback from the first stage. The frequency characteristic of the amplifier was linear at frequencies between 0 and 20 kc/s.

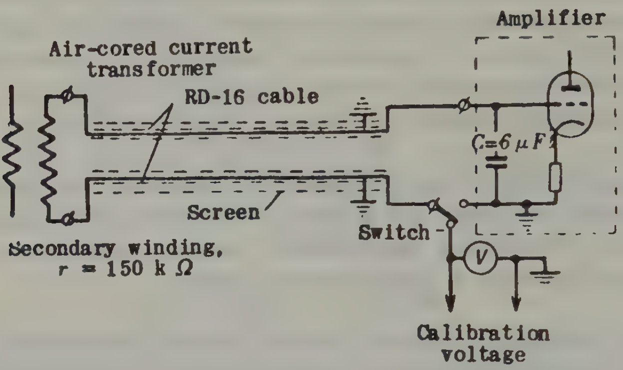
Amplifier
Air-cored current transformer
RD-16 cable
$C=6\mu F$
Screen
Switch
Secondary winding, $r = 150$ k Ω
V
Calibration voltage

Fig. 5. Calibration system for air-cored current transformer.

For calibration of the air-cored transformer, a special alternating voltage source was provided with tappings of 110, 300, 450 and 600 V together with a voltmeter which was also used as a null indicator for the d.c. output. Since the internal impedance of an air-cored transformer is large, resort to the circuit shown in Fig. 5 to introduce the calibration voltage into the secondary winding circuit was made. This system prevents the capacitance of the connecting conductors affecting the integrating circuit on calibration of the measuring circuit. If a calibration voltage of 50 c/s is switched on, the curve at the output of the amplifier is recorded on the electromagnetic oscillograph. The scale is found by the formula

$$m = \frac{2.82U_k}{314M2l} \text{ [kA/mm]},$$

where U_k is the calibration voltage, V;

M is the mutual inductance of the air-cored transformer, mH;

$2l$ is the amplitude envelope of the sine curve on the oscillogram.

The amplifier displayed a high degree of stability in operation.

Zero drift after a 10-min period of heating did not exceed 1 mA/hr.

Conclusions. Large transient currents can be successfully measured by integrating the current derivative.

2. The practical realization of the elements of the test equipment (the toroidal coil and amplifier) presents no great difficulty.

3. The turns of the toroidal coil must not exceed one tenth of the mean radius of the toroid in width, there must be about 30 turns per "running" centimetre of the winding circumference, and the toroidal coil must be made from a high impedance conductor so that the time constant of the coil L/R does not exceed 10 μ sec.

4. The integration time constant must not be less than 1 sec.

5. A simple d.c. amplifier can be used, preferably of the balanced type, with a gain S_a = 100 to 200 mA/V. Such an amplifier is stable in operation and has only a slight zero drift.

Translated by O.M. Blunn

A TWO MOTOR MACHINE-VALVE STAGE WITH SEMICONDUCTOR RECTIFIERS*

(Moscow Power Institute)

(Received 21 January 1961)

A good method of speed control for large plants of up to 10,000 kW or more in high-speed drives of 5000 to 7500 rev/min with a ventilator load is to use slip-ring motors in series.

Owing to the difficulties in constructing large high-speed slip-ring motors of about 3000 rev/min, it is expedient to use two smaller motors in place of one large motor. The increase in nominal speed leads to a saving in weight and cost of the motor and gearing. Thus, for example, use may be made of ATMF-2500-2, 2500 kW $\sim$ 3000 rev/min and ATMF-3700-2, 3700 kW $\sim$ 3000 rev/min slip-ring induction motors for the drives of centrifugal compressors of 7000 kW for gas mains (two motors to each plant).

In this paper a stage system is proposed (Fig. 1) in which the slip energy is converted by semiconductor (silicon or germanium) rectifiers (diodes) and then passed into the network.

The rectifiers are connected in two series-connected bridge systems. For the inverter group use is made of a constant speed unit consisting of a d.c. motor (DCM) and a synchronous generator (SM).

The shafts of the two asynchronous drive motors (1AM and 2AM) are rigidly connected to each other by couplings and to the working plant by a system of gears. The rotors of the asynchronous motors are shifted 30 el. deg. relative to each other.

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* *Elektrichestvo*, 8, 50-56, 1961.

Figs. 2 and 3 show oscillograms of the rectified voltages with and without such rotor displacement ($\theta = 0$ and $\theta = 30$ el. deg.). If $\theta = 0$, the pulsations of the variable component of the rectified voltages of the two bridges U_{d1} and U_{d2} coincide in phase and as a result of their addition the variable component of the total voltage U_d varies with the with the same frequency but twice the amplitude (Fig. 2).

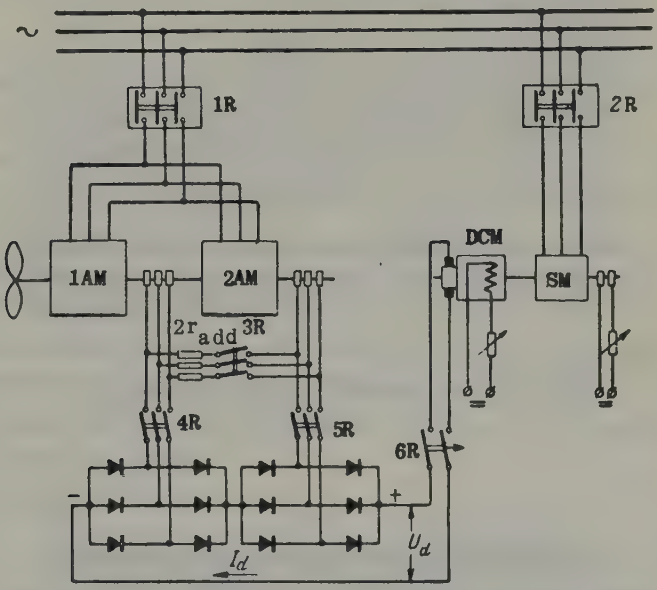

Fig. 1. Main circuit of two-motor machine-valve stage:
Test equipment: 1AM and 2AM – AK-61-6 type induction motors 7 kW, 220/380 V, 940 rev/min, cos $\varphi = 0.79$, $E_{2nom} = 175$ V, $I_{2nom} = 26$ A; DCM – d.c. motor type PN-45, 5.2 kW, 115 V, 45.2 A, $n_{nom} = 2000$ rev/min; SM – synchronous generator type SGS-4.5 kW, 230 V, 11.3 A, 1500 rev/min, cos $\varphi_{nom} = 0.8$; 1-6 R – switches; VK-25 type silicon rectifiers, 25 A, $U_{rev} = 150$ V.

If $\theta = 30$ el. deg. the pulsations of the variable component U_{d1} and U_{d2} are shifted in phase in such a way that the maximum of U_{d1} corresponds to the minimum of U_{d2}. This leads to a decrease in the amplitude of the variable component of the total rectified voltage U_d and a doubling of its frequency of pulsation (Fig. 3).

Analysis of the proposed system has shown that the two series-connected rectifier bridges operate independently in the given range of loads.

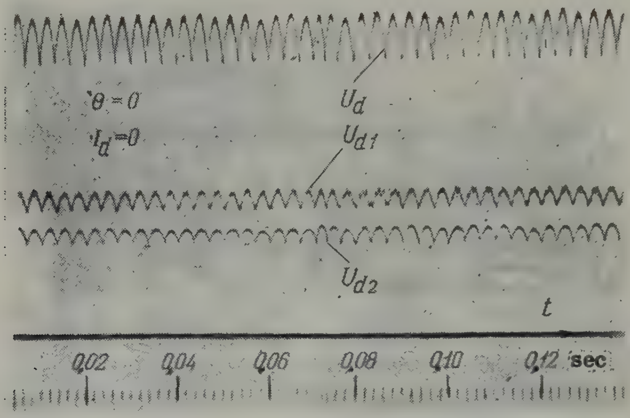

Fig. 2. Oscillogram of rectified voltages at θ = 0.

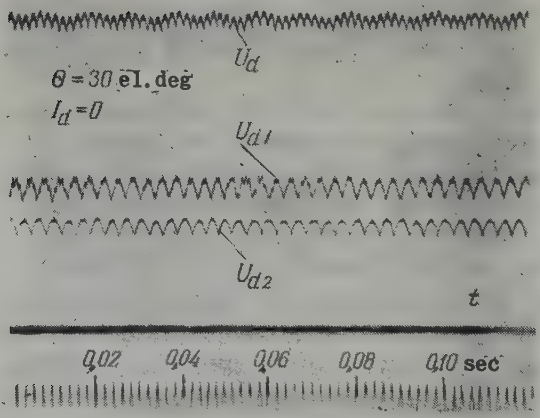

Fig. 3. Oscillogram of rectified voltages at θ = 30 el. deg.

An analogous system with one drive motor having two rotor windings shifted 30 el. deg. and six slip-rings was put forward by corresponding member of the U.S.S.R. Academy of Science D.A. Zavalishin.

The system is started by switching on the switch 1R in the stator circuits of the asynchronous motors with the rotor cross-connector closed. Given rotor shift through an angle θ, the rotor current is defined by the expression

$$I'_2 = U_1 \sqrt{\frac{1-\cos\theta}{2\left[\left(r_1 + c_1\frac{r'_{add}+r'_2}{s}\right)^2 + (x_1 + c_1 x'_2)^2\right]}}, \tag{1}$$

where I'_2 is the modulus of the current in the rotor of each motor, referred to the stator;

U_1 is the phase voltage of the stator;

r_1 and r'_2 are the phase resistance of the stator and the referred phase resistance of the rotor;

r'_{add} is half the additional resistance in the rotor circuits, referred to the stator winding;

x_1 and x'_2 are the phase inductive reactance of the stator and the referred phase inductive reactance of the rotor;

$c_1 = U_1/E_1$ is the ratio of the voltage to the e.m.f. of the motor at synchronous speed;

s is the motor slip.

The current consumed by the stator of each motor is defined as the vector sum of the no-load current and the referred rotor current of the corresponding motor.

Since the angles between the current vectors I'_2 and the corresponding e.m.f. of each motor are not the same, it follows that the motors will develop different torques

$$M_1 = \frac{M_{cr}(1+q)}{\frac{s}{s'_{cr}} + \frac{s'_{cr}}{s} + 2q}\left[(1-\cos\theta) - \frac{\sqrt{1-q^2}}{qs+s'_{cr}}\, s\sin\theta\right]; \tag{2}$$

$$M_2 = \frac{M_{cr}(1+q)}{\frac{s}{s'_{cr}} + \frac{s'_{cr}}{s} + 2q}\left[(1-\cos\theta) + \frac{\sqrt{1-q^2}}{qs+s'_{cr}}\, s\sin\theta\right], \tag{3}$$

where M_{cr} is the “critical” (pull-out) torque of the asynchronous motor in the normal system;

$s'_{\kappa} = \dfrac{c_1(r'_2 + r'_{\text{add}})}{\sqrt{r_1^2 + (x_1 + c_1 x'_2)^2}}$ — the critical slip; and

$$q = \frac{r_1}{\sqrt{r_1^2 + (x_1 + c_1 x'_2)^2}}. \tag{4}$$

The total torque of the two-motor drive on starting will be:

$$M = \frac{2M_{\text{cr}}(1 + q)}{\dfrac{s}{s'_{\text{cr}}} + \dfrac{s'_{\text{cr}}}{s} + 2q}(1 - \cos\theta). \tag{5}$$

Analysis of formula (5) shows that the initial torque of the drive on starting for $\theta = 30$ el. deg. attains 13.4 per cent of the initial starting moment of one motor.

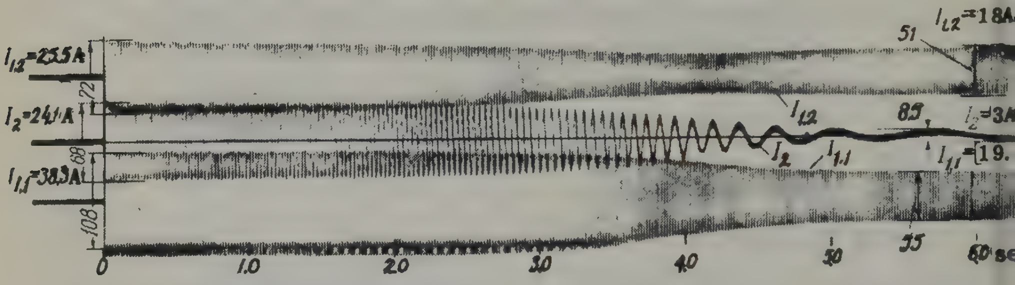

Fig. 4. Oscillogram of the starting of the two-motor drive.

When the starting torque is insufficient (starting on full load), provision may be made for shifting the rotor shafts through an angle $\theta = 90$ el. deg. In this case all the fundamental propositions in this paper remain valid.

Fig. 4 shows an oscillogram of the currents on starting for $\theta = 30$ el. deg.

By switching on switch 2*R* voltage is supplied to the stator of the synchronous machine and the constant speed unit accelerates to synchronous speed, after which excitation is applied to the d.c. motor. Voltage is supplied to the rectifiers when the asynchronous motors reach the steady state speed.

Regulation of the excitation current of the d.c. motor makes it possible to equate the voltages of the rectified circuit on the d.c. motor side as well as on the side of the rectifiers. After this, switch 6*R* is switched on in the rectified current circuit. At this the starting of the plant is finished.

Speed is controlled within wide limits by varying the excitation current of the d.c. motor.

Given the arbitrary slip s of two identical asynchronous motors and steady state operating conditions, the relationship between the voltage at the terminals of the d.c. motor and the rectified current will be (Fig. 5):

$$U_d = 2\left[2\sqrt{2}\,\frac{m}{\pi}\sin\frac{\pi}{m}E_{r0}s - \right.$$
$$\left. - I_d\left(\frac{mx_d s}{\pi} + 2r_d\right) - 2\Delta e\right], \qquad (6)$$

where U_d is the voltage at the terminals of the d.c. motor;

E_{r0} is the rotor phase voltage of the asynchronous motor at rest;

I_d is the mean value of the rectified current;

$m = 3$ the number of rotor phases;

x_d is the equivalent phase reactance of the asynchronous motor referred to the rotor circuit;

r_d is the equivalent phase resistance of the asynchronous motor referred to the rotor circuit;

s is the slip;

Δe is the voltage drop in one arm of the bridge.

The voltage U_d at the terminals of the d.c. motor will be:

$$U_d = E_d + I_d(r_a \pm B), \qquad (7)$$

where E_d is the e.m.f. of the d.c. motor due to the main excitation flux;

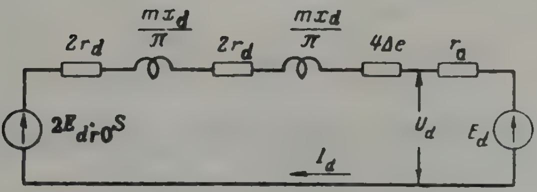

Fig. 5. Equivalent circuit.

r_a is the armature resistance of the d.c. motor;

B is a compounding factor having the dimension of resistance.

As a special case $B = 0$ for a d.c. motor with independent excitation and expression (7) takes the form:

$$U_d = E_d + I_d r_a. \tag{7a}$$

Substituting the expression for U_d from (7a) in (6):

$$2\left[E_{dr0}s - I_d\left(\frac{mx_d s}{\pi} + 2r_d\right) - 2\Delta e\right] - E_d - I_d r_a = 0, \tag{8}$$

where $E_{dr0} = 2\sqrt{2}\frac{m}{\pi}\sin\frac{\pi}{m}E_{r0}$ is the rectified voltage of the rotor with the motor stationary and the rectified circuit open ($I_d = 0$).

It follows from expression (8) that variation of E_d when the stage is operating at steady state speed leads to variation of the current I_d of the rectified circuit and, consequently, to variation of the torque M of the asynchronous motors.

If E_d increases, then I_d and therefore M decrease and the drive begins to slow down. The retardation will continue until equilibrium is attained in the rectified current circuit in accordance with equation (8).

In this case a definite current I_d is established along with a corresponding rotational torque M. The motors will rotate at less speed than before and at this lower speed the load torque will be balanced by the rotational torque of the motors. The system of increasing the speed is controlled in a similar way.

The relationship between the torque of the asynchronous motors and the rectified circuit current I_d is defined by the expression:

$$M = 2\frac{1}{\omega_0} I_d E_{dr0}\left(1 - \frac{I_d m x_d}{\pi E_{dr0}}\right), \tag{9}$$

where ω_0 is the synchronous angular velocity of the rotor.

It is possible to find the appropriate values of I_d and M for given values of s with E_d variable by formulae (8) and (9).

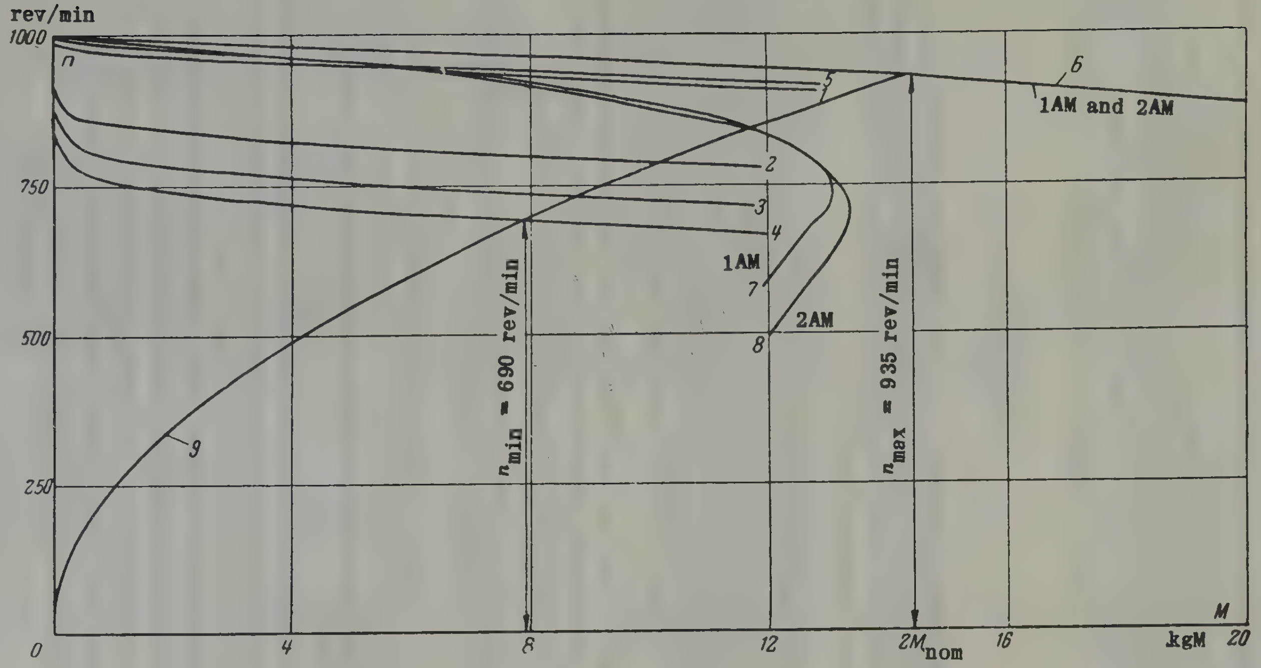

Fig. 6. Mechanical characteristics of the two-motor drive in the machine-valve stage. $s_{min} = 0.065$; $s_{max} = 0.31$; $U_{nom} = 220$ V.

Curves $n = f(M)$ can be plotted from this data for E_d = const. Other such curves can be found for the drive for different excitation currents of the d.c. motor and a family of curves is obtained. Fig. 6 shows a family of these curves obtained experimentally.

The initial slip corresponding to the ideal no-load speed of the drive when operating on the given $n = f(M)$ curve can be found from expression (8). Provided that $I_d = 0$, then

$$2\,[E_{dr0}s_0 - 2\Delta e] - E_d = 0, \tag{10}$$

whence

$$s_0 = \frac{E_d + 4\Delta e}{2E_{dr0}}. \tag{11}$$

It follows from expression (11) that with an increase of effected by regulating the excitation current of the d.c. motor, a reduction in the ideal no-load speed of the drive is achieved and the $n = f(M)$ curves thereby lowered.

It should be mentioned that the slip s_0 from equation (11) corresponds to the condition when the $n = f(M)$ curves of the drive are straight lines over their operating part. In fact at low loads the shape of the curves changes (Fig. 6) owing to the interruption of the current and the initial slip will be less than the calculated figure.

The maximum torque of the drive for the various d.c. motor excitation currents is of the same value equal to the maximum torques of the two drive motors in operation on "natural" $n = f(M)$ curves.

The critical slip of an "artificial" (equivalent) curve for the two-motor drive is found by the formula:

$$s_{cr} = (1 + \rho \pm \beta)\, s_{cr} + \sqrt{2}\, s_0, \tag{12}$$

where s_{cr} is the critical slip of the two-motor drive on natural curves;

γ and β are relative values of the d.c. motor armature resistance and the compounding factor referred to the stator circuit.

As a special case $\beta = 0$ for a d.c. motor with independent excitation and expression (12) accordingly takes the form:

$$s_{cr} = (1 + \rho)\, s_{cr} + \sqrt{2}\, s_0, \tag{13}$$

$$\rho = \frac{\sqrt{\frac{3}{2}\, k_e^2 r_a}}{2\sqrt{2}\,\frac{m}{\pi}\sin\frac{\pi}{m}\cdot r_2'} = 0.53 k_e^2 \left(\frac{r_a}{r_2'}\right), \tag{14}$$

where k_e is the coefficient of transformation from the stator to the rotor.

In determining s'_{cr} (the artificial curve critical slip) from formulae (12) and (13) the effect of overlap of the rectifier anodes was ignored. Unlike that obtained by calculation, the critical slip will in fact be slightly greater as has been confirmed by tests.

Curve 1 in Fig. 6 represents the fundamental $n = f(M)$ curve of the stage when the d.c. motor excitation current is zero. In this case the initial slip is defined by the sum of the d.c. motor e.m.f. $E_{d\mu}$ from residual magnetism and the voltage drop $4\Delta e$ in the rectifiers:

$$s_{01} = \frac{E_{d\mu} + 4\Delta e}{2E_{dr0}}. \tag{15}$$

Curve 4 in Fig. 6 corresponds to the rated excitation current of the d.c. motor. Curves 2 and 3 refer to intermediate values of the excitation current.

In order to increase the speed of the drive and thereby widen the range of control and increase the utility of the drive, it is necessary to reverse the polarity of the d.c. motor excitation circuit. In this case the excitation current changes sign and at a certain value the e.m.f. $E_{d\mu}$ of residual magnetism will be compensated. The formula for the initial slip takes the form:

$$s_{05} = \frac{4\Delta e}{2E_{dr0}}. \tag{16}$$

These are the conditions to which curve 5 in Fig. 6 refers.

With a further increase in the negative value of the d.c. motor excitation current a moment occurs when each rectifier transmits a current in the same direction during a complete period. The explanation is that a negative potential is applied to the common cathodes of the three rectifiers in each bridge and the magnitude of this potential exceeds the negative potential of the anodes of two rectifiers (the anode of the third rectifier has a positive potential at this moment) Likewise, a positive potential is applied to the common anodes of the other three rectifiers in each bridge and the magnitude of this positive potential exceeds the positive potential of the cathodes of two rectifiers. As a result shorting takes place through the rectifiers of the three phases of the rotor windings of the asynchronous motors.

Asynchronous motors which are connected in stages are independent of it on their natural $n = f(M)$ curves.

The d.c. motor operates as a generator. Its comparatively small e.m.f. fully covers the voltage drop in the rectified current circuit. The magnitude of the current in the rectified circuit remains constant during load variations and depends on the d.c. motor excitation current. The synchronous machine operates as a motor consuming energy from the mains and drives the d.c. motor.

Under these conditions

$$E_d = I_d r_a + 4\Delta e \tag{17}$$

and

$$s_0 = 0. \tag{18}$$

In this case the $n = f(M)$ curve of the stage corresponds with the natural curve 6 of a two-motor drive, which is the sum of the natural $n = f(M)$ curves of the two individual motors (7 and 8).

The range of speed control can be widened by about 4 to 5 per cent by reversing the polarity and applying negative excitation to the d.c. motor.

Curve 9 in Fig. 6 represents the torque-speed curve for a ventilator load. The intersection of curve 9 with curves 6 and 4 defines the limits of speed control.

A comparatively low range of speed control was obtained on the test equipment (from 100 to 74 per cent). But under real conditions the range of speed control can be widened considerably by selecting the parameters of the electrical equipment in the stage appropriately.

The rating of the proposed machine in the stage under consideration is defined by the product of the maximum current (at maximum speed of the drive) times the maximum voltage (at minimum speed of the drive). In large plants with ventilator loads it is therefore advisable to use two d.c. machines, switching them from parallel connexion to series connexion. This permits a substantial reduction in the rating of the d.c. machines. For example, for speed control of the drive from 100 to 50 per cent, the use of two d.c. machines with such switching roughly halves the rating of these machines (approximately 25 per cent of the established power of the drive motors instead of about 50 per cent without switching).

The rating of the synchronous machine is equal to the square root of the squares of the active and reactive power. The active power is defined as the maximum value of the regenerative active power, which is no more than about 15 per cent of the established power of the drive motors for the drive of gear with a ventilator load. The reactive power of the synchronous machine depends on the need to compensate the reactive power of the electric drive, i.e. the need to improve its power factor.

The expediency of using asynchronous slip-ring motors in stages to regulate the speed of mechanisms with a ventilator load has been confirmed by comparing the technical and economic indices of the proposed system with those of a system with rheostat control.

The comparison was made for a 6300 kW compressor on the basis of the annual consumption graph at the Ukrgiprogaz gas works.

Tables 1 and 2 refer to the rheostat control system.

TABLE 1

No. of hr worked in year	Rotational speed, %	Useful power on shaft, kW	Total power loss, kW	Power consumed from network, kW	Efficiency, %	Utilized energy, mill, kW/hr	Expenditure of electrical energy, mill, kW/hr
2.160	100	6.430	240	6.670	96.5	13.9	14.4
1.464	92.5	5.060	570	5.630	89.8	7.41	8.25
1.464	85	3.970	845	4.815	82.5	5.81	7.05
2.184	77.5	3.020	1.015	4.035	74.7	6.60	8.82
1.488	70	2.250	1.080	3.330	67.2	3.34	4.95

TABLE 2

Cost of main electrical equipment, C_{eq}, 1000 roubles	Cost of erection, C_{er}, 1000 roubles	Depreciation, C_d, 1000 roubles	Repair costs C_{rep}, 1000 roubles	Cost of electrical energy C_e 1000 roubles	Capital outlay C_{outl}, 1000 roubles	Operating costs C_{opl}, 1000 roubles
51.6	5.16	5.16	1.55	351	56.76	357.71

Total electricity consumption was 43.47 million kW/hr per annum. Annual losses amounted to 6.41 million kW/hr, leaving 37.06 million kW/hr usefully used. The mean annual efficiency was 85.2 per cent.

The cost of the main equipment was as follows: motor 6500 kW, approx. 1500 rev/min – 41.0 thousand rubles; control resistors and the "magnetic station" (contactors) – 3.5 thousand rubles; "KRU" chambers (compact bus, breaker and switchboard cubicles) – 0.5 thousand roubles; static capacitors (232 items) – 6.6 thousand rubles.

The number of static capacitors depended on the need to ensure cos $\varphi = 0.92$. The cost of 1 kW/hr of electrical energy was taken as 0.8 kopecs.

$$C_{er} = 0.1C_{eq}; \quad C_d = 0.1C_{eq}; \quad C_{rep} = 0.03C_{eq};$$
$$C_{out1} = C_{eq} + C_{er}; \quad C_{op1} = C_d + C_{rep} + C_{el}.$$

Here the subscripts refer to various items of cost C, namely, er – erection, eq – equipment, rep – repairs, out 1 – capital outlay on first system, op 1 – operating costs, d depreciation, and e – electricity.

Tables 3 and 4 refer to the same system but with rheostat control replaced by the two-motor machine-valve stage with semiconductor rectifiers.

TABLE 3

No. of hr worked in year	Rotational speed, %	Useful power on shaft, kW	Total power loss, kW	Power consumed from network, kW	Efficiency, %	Utilized energy, mill. kW/hr	Consumption of electrical energy, mill, kW/hr
2,160	100	6,430	430	6,860	93.6	13.90	14.82
1,464	92.5	5,060	420	5,480	92.4	7.41	8.02
1,464	85	3,970	405	4,375	90.7	5.81	6.41
2.184	77.5	3,020	390	3,410	88.5	6.60	7.45
1,488	70	2,250	375	2,625	85.7	3.34	3.90

The useful electricity was the same as with rheostat control, but the actual consumption was 40.60 million kW/hr, i.e. 2.87 million kW/hr less, and the mean annual efficiency was 91.1 per cent. It should be pointed out that the total power loss at maximum (rated) speed can be reduced to 240 kW (the rated loss of asynchronous motors) by disconnecting the stage when the motors are operating on the natural $n = f(M)$ curve.

TABLE 4

Cost of main electrical equipment, C_{eq}, 1000 roubles	Cost of erection C_{er}, 1000 roubles	Increased cost of building for extra equipment, C_b, 1000 roubles	Depreciations C_d, 1000 roubles	Repair costs C_{rep}, 1000 roubles	Cost of electrical energy C_e, 1000 roubles	Capital outlay, C_{out2}, 1000 roubles	Operating costs C_{op2}, 1000 roubles
76.8	11.5	5.0	7.68	2.32	325	93.3	335

To determine the cost of the drive motors it was assumed that the power of the two drive motors was the same and equal to 3500 kW. The cost of the main equipment C_{eq} includes all the machines and electrical gear in the drive system:

$$C_{er} = 0.15C_{eq}; \quad C_d = 0.1C_{eq}; \quad C_{rep} = 0.03C_{eq};$$
$$C_{out2} = C_0 + C_{er} + C_b; \quad C_{op2} = C_d + C_{rep} + C_e.$$

where the subscripts are the same, except that "2" refers to the two-motor system and b to the extra cost of altering the buildings.

The time taken for the prime cost of the twin-motor system to be recovered out of reduced running costs can be found from the expression

$$T = \frac{C_{out2} - C_{out1}}{C_{op1} - C_{op2}} = \frac{93.3 - 56.76}{357.71 - 335} = 1.61 \text{ yr.}$$

The use of two drive motors instead of one large motor in the proposed system has the following advantages:

(a) the weight of the motors and gearing is reduced and rated speed increased;

(b) the motors can be started without a starter rheostat since if the rotors are electrically displaced by $\theta = 30^{\circ}$, a balancing e.m.f. arises in the circuit of the rotors and the starting torque is sufficient to accelerate the motor up to full speed (for no-load starting); in this case the change in current is below the maximum.

(c) smooth curves can be obtained for the rectified current and voltage since in this case there is 12-phase rectification. There is no further need of a smoothing choke in the plant.

The use of semiconductor rectifiers in place of mercury arc rectifiers allows a considerable reduction in the loss of electrical energy, i.e. increases the efficiency of the plant, and reduces its dimensions.

Translated by O.M. Blunn

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A STUDY OF THE E.M.F. INDUCED IN ELECTRICAL MACHINES USING AN ELECTROLYTIC TANK*

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In some cases in the design of electrical machines it is necessary to determine the shape of curves for the e.m.f. induced in the individual windings, sections or conductors with the greatest possible accuracy. Above all, this is the case with heavy duty d.c. machines (especially with complex multi-path armature windings), synchronous machines with strictly sinusoidal voltage curves inductor machines, and electromagnetic stepping devices etc. It may also be necessary to determine the shape of the e.m.f. curves for the analysis of secondary phenomena and behaviour in machines, e.g. commutation, balancing currents, additional losses etc.

A rigorous analytical approach to the problem is as a rule impossible owing to the complex configuration of the magnetic systems (beak shaped poles, stator and rotor teeth), whilst approximate methods tend to be too rough or too cumbersome. This puts a premium on finding other methods of investigation. It should be pointed out that the e.m.f. of sections, coil groups and whole windings can easily be found if the e.m.f. in the individual winding conductors are known. Thus, the problem of investigating the e.m.f. in systems of conductors can always be reduced to the determination of the e.m.f. in the individual conductors.

The shape of the curve for the e.m.f. in an armature conductor of an electrical machine can always be found quite easily, provided no tooth configuration is present, if the distribution of the normal component of the magnetic induction vector B_n is known over the surface of rotation of the conductor.

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* *Elektrichestvo,* 8, 62-66, 1961.

No re-distribution of the magnetic field occurs on rotation of the rotor owing to the smoothness of the surface and it can therefore be seen from the formula which holds in this case

$$e = B_n lv \tag{1}$$

that if the conductor intersects the lines of magnetic induction at a constant speed, then the shape of the e.m.f. curve will coincide in shape with that for the distribution B_n over the surface under consideration (rotor or stator, depending on where the conductor is placed).

The picture is quite different if the surface is not smooth. A re-distribution of the magnetic field takes place inside the gap if teeth are present, i.e. the shape of the curve for the distribution of B_n changes as a function of the position of the rotor and owing to the continuous re-distribution of the lines of induction on rotation of the rotor, the velocity v in formula (1) cannot be regarded as constant.

It is therefore rather a question of the resemblance of the two curves and not their identity.

The effect of the different factors on the shape of the e.m.f. curve can be investigated on an actual machine, but it is hardly practicable owing to the cost and difficulty of obtaining the various different versions of the machine which are required. This is an insurmountable difficulty in the case of some machines which are to be re-designed. The electrolytic tank method which is described in this paper is sometimes the only way that the shape of the e.m.f. curve can be determined.

The intensity of the induced electrical field can be described by the expression

$$\mathbf{E} = -\frac{\partial \mathbf{A}}{\partial t}, \tag{2}$$

where A is the vector potential of the magnetic field.

If the magnetic field on the active length of the machine is regarded as two-dimensional, then instead of equation (2)

$$e = -l\frac{dA_z}{dt}, \tag{3}$$

where e is the e.m.f. in the conductor, l is the active length of the conductor in the slot, and A_z the component of the vector potential of the magnetic field along the z axis of the machine.

The vector potential is defined by all the currents of the machine. Variation with time may take place if the conductor under investigation is moved relative to the magnetic field (during time dt the conductor moves from the point with the potential A to a point with the potential $A + dA$) and if a change takes place in the currents in the machine windings (in this case the quantity A varies with time at the point in question).

No special difficulties are involved in modelling for the second case. Attention will therefore be paid to the first case on the assumption that the currents are constant. In this case an e.m.f. only arises in the conductors due to their movement in the magnetic field.

It is assumed that the rotation of the rotor takes place with a constant angular velocity ω. Then

$$v = \omega r = r\frac{d\alpha}{dt}, \tag{4}$$

where v is the speed of the conductor at a distance r from the axis of rotation, $\omega = 2\pi n$ the angular velocity of rotation of the rotor and $d\alpha$ the angle of rotation of the rotor during time dt.

From expressions (3) and (4)

$$e = -l\frac{dA_z}{d\alpha}2\pi n. \tag{5}$$

The principles underlying direct investigation of e.m.f. by modelling in an electrolytic tank follow from formula (5).

In order to model the e.m.f., it is sufficient to model the field of the vector potential A_z and imitate the motion of the rotor relative to the stator.

For the current field in a conducting medium

$$\operatorname{div} \delta = 0, \quad \operatorname{rot} \mathbf{E} = 0.$$

If we put

$$\delta = \delta_1 + \delta',$$

where $\delta_1 = \gamma E$ is the density of the current in the electrolyte and δ' the density of the current from the supply source, we can then write:

$$-\operatorname{div} \delta' = \operatorname{div} \delta_1 = p$$

and if

$$E = -\operatorname{grad} U, \tag{6}$$

we then finally have

$$\operatorname{div} \operatorname{grad} U = -\frac{p}{\gamma} \quad \text{or} \quad \operatorname{div} \frac{1}{\rho} \operatorname{grad} U = -p;$$

where p is the volume density of the current introduced into the bath from an extraneous source (the supply source).

Using the identity div B = 0, equivalent to the identity B = rot A, the following set of equations for the magnetic field

$$\operatorname{rot} \mathbf{H} = \delta, \ \operatorname{div} \mathbf{B} = 0, \ \mathbf{B} = \mu \mathbf{H},$$

simplifies to the form

$$\operatorname{rot} \operatorname{rot} \mathbf{A} = \mu \delta.$$

For a plane field in a Cartesian system of co-ordinates

$$\operatorname{div} \operatorname{grad} A_z = -\mu \delta_z \text{ or } \operatorname{div} \frac{1}{\mu} \operatorname{grad} A_z = -\delta_z. \tag{7}$$

Using the sameness of expressions (6) and (7), it can be asserted if the equality of the boundary conditions is observed (geometric similarity and correspondence of the medium with the magnetic permeability μ and electrical conductance ρ) that the distribution of the electrical potential in the bath will be analogous to the distribution of the vector potential in the machine. In this case, the lines of equal potential U = const in the model will coincide with the lines of induction in the machine.

This can be seen from a comparison of the expressions

$$\mathbf{E} = -\operatorname{grad} U = -\mathbf{i}\frac{\partial U}{\partial x} - \mathbf{j}\frac{\partial U}{\partial y}$$

and

$$\mathbf{B} = \operatorname{rot} \mathbf{A} = \mathbf{i}\frac{\partial A_z}{\partial y} - \mathbf{j}\frac{\partial A_z}{\partial x} = c_1\left(\mathbf{i}\frac{\partial U}{\partial y} - \mathbf{j}\frac{\partial U}{\partial x}\right).$$

The vector **E** is transverse to the lines U = const and the vector **B** is transverse to the vector **E**. Consequently, the vector **B** is tangential to the equipotential lines. Thus, the modelling of the field of vector **A** can be reduced to the construction of a geometrically similar model where the regions with different μ correspond to regions with proportional values of ρ. Here the eddy zone ($\delta \neq 0$) is modelled by a geometrically similar zone where the current is introduced from an external source. The magnitude of the current in the model must here be proportional to the current in the original.

The motion of the rotor in the bath can be simulated by successive rotation of the rotor model through the angle $\Delta\alpha$ about its axis. On rotation of the rotor a finite increment in the angle $\Delta\alpha$ corresponds to a finite increment in the potential ΔU and, consequently, $\Delta A_z = c\Delta U$ also. Thus, we finally have for determining the e.m.f. by modelling

$$e = -cl \cdot 2\pi n \frac{\Delta U}{\Delta\alpha}. \tag{8}$$

Quantitative values of the e.m.f. can be obtained if we know the value of c. This can be found in the following way. Suppose we put $A = c_1 U$, $x_1 = c_2 x_2$, $y_1 = c_2 y_2$, $\delta = c_3 p$, $\mu = c_4 \rho$ and consider that for a homogeneous medium

$$\Delta U = \frac{\partial^2 U}{\partial x_2^2} + \frac{\partial^2 U}{\partial y_2^2} = -\rho p. \tag{9}$$

Then for equation (7) (also in the case of a homogeneous medium)

$$\frac{\partial^2 A_z}{\partial x_1^2} + \frac{\partial^2 A_z}{\partial y_1^2} = -\mu\delta_z$$

or

$$\frac{\partial^2(c_1U)}{\partial(c_2x_2)^2}+\frac{\partial^2(cU)}{\partial(c_2y_2)^2}=-c_4\rho c_3p,$$

i.e.

$$\frac{c_1}{c_2^2c_3c_4}\left(\frac{\partial^2U}{\partial x_2^2}+\frac{\partial^2U}{\partial y_2^2}\right)=-\rho p.$$

Thus, in accordance with equation (9), we get

$$\frac{c_1}{c_2^2c_3c_4}=1;\quad c_1=c_2^2\,c_3c_4.$$

The similarity coefficients are defined by the modelling conditions, namely, c_2 by the modelling scale, c_3 from the correspondence of the currents and c_4 from the correspondence of the media. Knowledge of the coefficient of correspondence c_1 ensures that quantitative data are not only obtained for calculating the e.m.f., but also for finding the distribution of the fluxes in the "window" of the machine, the coefficient of self-induction, the magnetic permeance and so on.

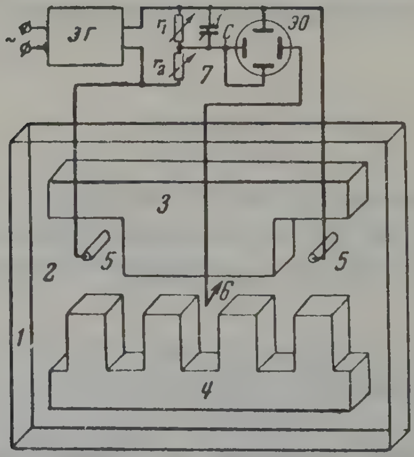

Fig. 1. Test equipment:
1 – tank; *2* – electrolyte; *3* – model of stator; *4* – model of rotor; *5* – current leads; *6* – probe; *7* – measuring circuit.

For example, in order to determine the flux in some circuit,

$$\Phi = \oint A\,dl = l_1(A_1 - A_2) = c_2 l_2 c_1 (U_1 - U_2), \qquad (10)$$

where U_1 and U_2 are the potentials of the conductors or imaginary lines along which the conductors of length l_2 are arranged. The total flux coupling can be found from the formula

$$\Psi = \sum_{i=1}^{i=n} \Phi_i = \sum_{i=1}^{i=n} \oint A_i\,dl = c_2 l_2 c_1 \sum_{i=1}^{i=n} (U_i - U_i'). \qquad (11)$$

If it is a question of determining the flux coupling of the coil producing the magnetic field in question, then U_i are the potentials of the incoming current leads, and U_i' the potentials of the outgoing current leads which imitate the currents in the turns of the coil. The flux coupling of mutual induction can be found in a similar way but U_i and U_i' are taken as points where the sides of the turns of the other coil are disposed. In such conditions the coefficients L and M can easily be defined as

$$L = \frac{\Psi_l}{i}; \quad M = \frac{\Psi_M}{i}.$$

The test equipment for investigating the e.m.f. (Fig. 1) is essentially the same as Tetelbaum's for investigating the potential distribution in a conducting medium [1]. But certain undersirable phenomena may occur on movement of the model of the rotor in the tank. The electrolyte may penetrate underneath the model of the rotor. This sharply distorts the nature of the field distribution in the zone with teeth since this is the most likely place for the electrolyte to flow. In a conventional fixed model this phenomenon can be avoided quite easily be greasing the slit by a non-conducting substance such as vaseline. Experience has, however, shown that a layer of vaseline between the rotor and the bottom of the tank does not guarantee that the electrolyte will be kept out. It is therefore proposed to solve the problem by a special construction of the model. In this construction (Fig. 2b) the rotor is permanently fixed on a sheet which is free to slide along the bottom of the tank. Between the moving sheet and the bottom of the tank it is necessary to have a layer of vaseline in order to keep out the electrolyte and reduce friction. The face of the moving sheet is also covered with a layer of vaseline and slides along the face of the fixed sheet of the same thickness on which the model of the stator is secured. This method of construction in practice precludes the penetration of electrolyte underneath the model of the rotor.

Another difficulty of a purely constructional nature is the need to ensure very exact rotation of the rotor by the given angle $\Delta\alpha$. Since the potential varies sharply in the zone of the gaps, inaccurate rotation can lead to considerable errors in determining the potential differences. In this respect it is advisable to measure the linear displacement of the rotor and not the angle of rotation $\Delta\alpha$. This can be measured by a micrometer.

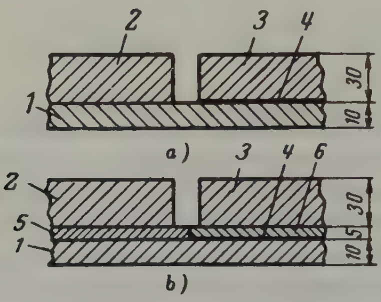

Fig. 2. Alternative versions of test equipment:
1 – bottom of tank; *2* – model of stator;
3 – model of rotor; *4* – layer of vaseline;
5 – base of stator model; *6* – base of rotor model.

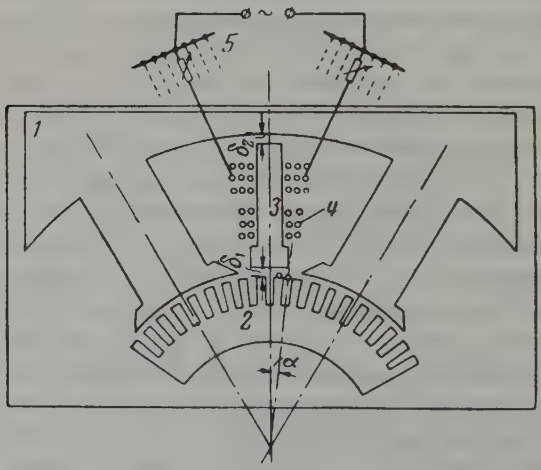

Fig. 3. Drawing of model:
1 – model of stator magnetic system; *2* – model of rotor; *3* – model of compole; *4* – current leads modelling the compole winding; *5* – resistances.

If these precautionary measures are taken quite stable and accurate results can be obtained.

To illustrate the use of the proposed method, suppose it is required to determine the shape of the curve for the commutating e.m.f. induced by the compole field in a conductor which is set in an armature slot of a d.c. machine.

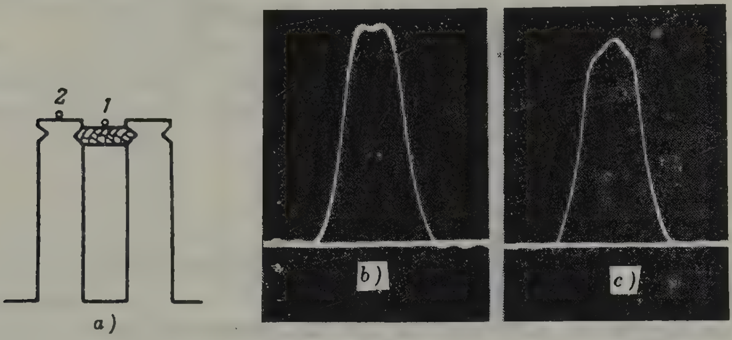

Fig. 4. Arrangement of measuring conductors (a); e.m.f. induced by compole field in conductor 1 (b) and conductor 2 (c).

The measurements are taken on a model whose dimensions fully correspond with the dimensions of a real 118 kW, 440 V, 1000 rev/min machine (type GM-282) operating as a motor. The model is drawn in Fig. 3. The tank dimensions are 700 x 400 x 100 mm^3 so that it can contain a life-size model of about one quarter of the circumference of the machine. The model itself is made of organic glass 30 mm thick. A pointer micrometer was used to measure the rotor travel (with calibration 0,01 mm).

Parallel with the investigations in the tank, oscillograms were taken of the e.m.f. induced by the field of the compoles in special measuring conductors laid on the rotor of the GM-282 machine. The position of these conductors is illustrated in Fig. 4a, one being on a wedge along the slot, and the other on top of the tooth. Oscillograms of the e.m.f. in the conductors are shown in Fig. 4b, c. The e.m.f. in the first conductor (the curve in Fig. 4b) can be regarded as the commutating e.m.f. induced in the conductors of the sections covered by the brushes.

The curve for the commutating e.m.f. obtained by oscillograph recordings is compared with that obtained by tank measurements in Fig. 5a and b. It will be seen from this diagram that quite satisfactory agreement is obtained. The slight discrepancy between them can be explained by the slight difference between the dimensions of the model and those of the original, the residual magnetization of the main poles, present in the machine, but absent in the model (Fig. 4b,c), and the saturation of the peaks of the pole tips.

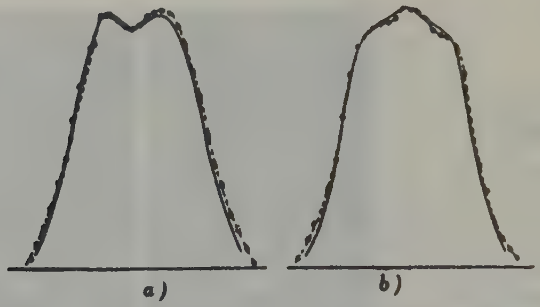

Fig. 5. Comparison of curves obtained by oscillograph (continuous line) and by modelling in tank (broken line).

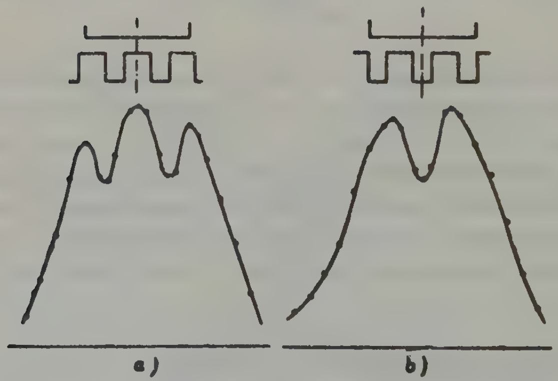

Fig. 6. Distribution of the normal component of induction under compole in modelling.

In addition, the shape of the curve for the e.m.f. which would be induced in a conductor laid up the middle of the rotor tooth was also determined on the model, i.e. the corresponding curve to that in Fig. 4c was determined. The comparison of these curves in Fig. 5b also reveals good agreement.

It is worthwhile mentioning, however, that the curves for the distribution of the component of the magnetic induction in the gap at a distance of 1 mm from the surface of the rotor as determined by the model in the tank for two rotor positions, differ considerably in shape from those for the e.m.f.'s induced in the conductors 2 and 1 on rotation.

It can be seen from the foregoing example that the proposed method is a comparatively easy and quick way of finding the rotational e.m.f. in quite complicated conditions and that the results are acceptably accurate. No difficulties arise in principle in applying the method to more complicated cases than that of the excitation of a single compole, which was only used by way of illustration.

Conclusions

1. The proposed method of determining the shape of the curves for the rotational e.m.f.'s in electrical machines by modelling in an electrolytic tank is a comparatively easy way of investigating complicated magnetic circuits and current distributions in electrical machines.

2. The test equipment which has been evolved provides quite stable and accurate results.

3. Good agreement was obtained between actual oscillograms and tank-determined curves.

4. The test equipment can also be used for determining all the other parameters of the magnetic field in a machine: induction, magnetic fluxes, flux coupling, inductances, mutual inductances and so on.

Translated by O.M. Blunn

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ABSTRACTS FROM PAPERS PUBLISHED IN ELEKTRICHESTVO No. 8, 1961

Computers

Electronic simulation of electrical circuits,
V.G. Vasil'ev *et al.*, (pp 41-45).

Certain aspects of the simulation of R, L, and C circuits are considered on the basis of a simple example. The rules of the "dual circuit" method of analysis are formulated. An electrical circuit with one voltage source is simulated without forming equations for the circuit or the model. A minimum of amplifiers and integrating elements is required.

Regulation of the excitation of two synchronous machines operating in parallel, G.V. Mikhnevich *et al.*, (pp. 31-35).

An analogue computer method is proposed for determining the optimum structure of the control signals with respect to their derivatives and assessing their effect on a coefficient of regulation for the excitation of two synchronous machines operating in parallel. The results for generators at the Stalingrad hydro-electric station are given by way of illustration.

Control engineering

The motor drive of a reversible cold rolling mill,
I.M. Tolmach *et al.*, (pp.79-81).

An account is given of the motor drive which has been installed on a four-roll mill for low and medium carbon steel strip up to 300 mm wide with reduction from 3 to 0.2 mm in pu to nine passes. The drive features the use of e.m.f. and current regulators to maintain rolling tension.

Measuring amplifiers for centralized automatic control systems, R.R. Kharchenko *et al.*, (pp. 7-13).

Attention is concentrated on those d.c. measuring amplifiers which are used to convert the signals of pick-ups in group data-processing circuits. The authors review six types of system covering the combined transmission of the amplified signal without a device for correcting zero, drift, and wideband amplifiers with a device for correcting zero drift.

Electrical steel

The properties of 1 mm cold-rolled electrical steel, A.I. Beliakov *et al.*, (pp. 82-83).

A short account is given of the properties of the 1 mm gauge cold-rolled grain-oriented steel for the poles of d.c. electrical machines now being produced in Novosibirsk.

Insulation

Interdependence of the physical properties of organic and inorganic polymers, N.I. Vorob'ev *et al.*, (pp. 76-78).

To simplify the problem of investigating the large number of polymers which are available, the author takes the magnitude of the inter-molecular forces as the main criterion defining the physical properties of many polymer substances.

The dielectric permeability and tangent of the loss angle for paper dielectrics, S.K. Medvedev, (pp. 66-72).

Formulae are produced for calculating ϵ, tan δ and other characteristics of paper on the basis of the actual structure of the paper on the assumption that the relative thickness of the cellulose in the space between the layers varies between 0 and 1 according to a certain law. The pores are assumed to be in series in each individual volume of cellulose.

Power systems

Determining optimum operating conditions for power systems, V.M. Gornshtein, (pp. 19-24).

A study is made of common errors which are said to be made in the economic analysis of power system analysis.

Tuned power transmission systems, V.K. Shcherbakov (pp.25-30).

It is stated that theory and laboratory investigations indicate that half-wave tuned transmission lines of 2000 and 2500 km can ensure high steady state stability and relative voltage stability at line ends. The author disputes the reasons which have been given for not using this system and advocates its use on the grounds of efficiency and the possibility of intermediate off-take of power along the line.

Rotating machines

Turbo-generators of large unit capacity, V.V. Titov, (pp. 1-4).

A short account is given of the work which has been done at the Electrosila works in the U.S.S.R. on the development of 300 and 500 MW turbo-generators. The new 300 MW generators are described and it is stated that 500 MW generators can be made on the same principles when the demand arises. Extremely high efficiency is claimed and it is an interesting paper. Unfortunately, this account does not explain how the efficiency is achieved or how it is calculated and some of the main features have their counterpart in recent American, Swiss and British innovations.

New 100 kW amplidynes, B.F. Tokarev *et al.*, (pp. 14-18).

Two new types of amplidyne are to be produced in the U.S.S.R., namely, three-stage amplidynes with a longitudinal field, and multi-pole amplidynes with a transverse field (four and eight poles) with increased magnetization of the winding along the transverse axis or with additional poles along the longitudinal and transverse axes. The equipment is compared and the respective merits assessed.

A new principle of obtaining constant advance times in automatic synchronizers, N.N. Vostroknutov *et al.*, (pp.35-40).

In this article the synchronizers are defined as devices which select the angle of lead and stop action if the amount of slip is too large. A new approach to the design of the time advance elements of such synchronizers is proposed in which the angle of lead is directly converted into a proportional d.c. voltage. This provides the possibility of twice-differentiating this voltage, as required for considering acceleration of slip. A detailed account is given of a device for the exact synchronization of generators.

Calculation of the steady state errors in speed governors for d.c. motors, S.S. Roizen, (pp. 56-62).

Two fundamental methods of speed control for d.c. motors are considered with a view to determining the accuracy with which speed governors operate in the automatically controlled drives of rolling mills and lathes etc. Formulae are produced for calculating the degree of error under steady state conditions.

The leader stage of a spark discharge, V.P. Larionov, (pp. 72-76).

The author develops the notion of a leader stage in spark discharges in long air-gaps which precedes the main discharge and forms a leader channel between the electrodes. The mean longitudinal potential gradients in the leader channel are determined along with the conductance of the leader channel, the concentration of the electrons and the time necessary for the formation of the leader channel.

Translated by O.M. Blunn

A METHOD OF DETERMINING THE MAIN CRITERIA OF TRACTION MACHINES FOR MINIMUM WEIGHT PER UNIT OF OUTPUT*

N.A. BOLDOV

(Moscow Power Institute)

(Received 9 January 1961)

A reduction in the weight of traction machines per unit of output allows a considerable increase in the power of electric locomotives for a given weight and speed or, other things being equal, permits a substantial saving in metal in the construction of locomotives and coaches etc.

This paper describes a method of determining the optimum criteria of traction machines and considers the fundamental relationships involved. Improved formulae will be given, including an initial approximate formulae for the weight of electronic machines. This particular formulae is the outcome of investigations by V.G. Yegorov and the author in the department of electrical transport at the Moscow Power Institute.

The dimensions and weight of traction machines. A comparison of the dimensional and weight indices of 35 different d.c. and a.c. traction motors and traction generators has indicated the following empirical formula:

$$G=\frac{62}{p}D_a^2\,l_a\ [\text{tons}], \tag{1}$$

where G is the weight of the motor or generator;

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* *Elektrichestvo,* 9, 17-21, 1961.

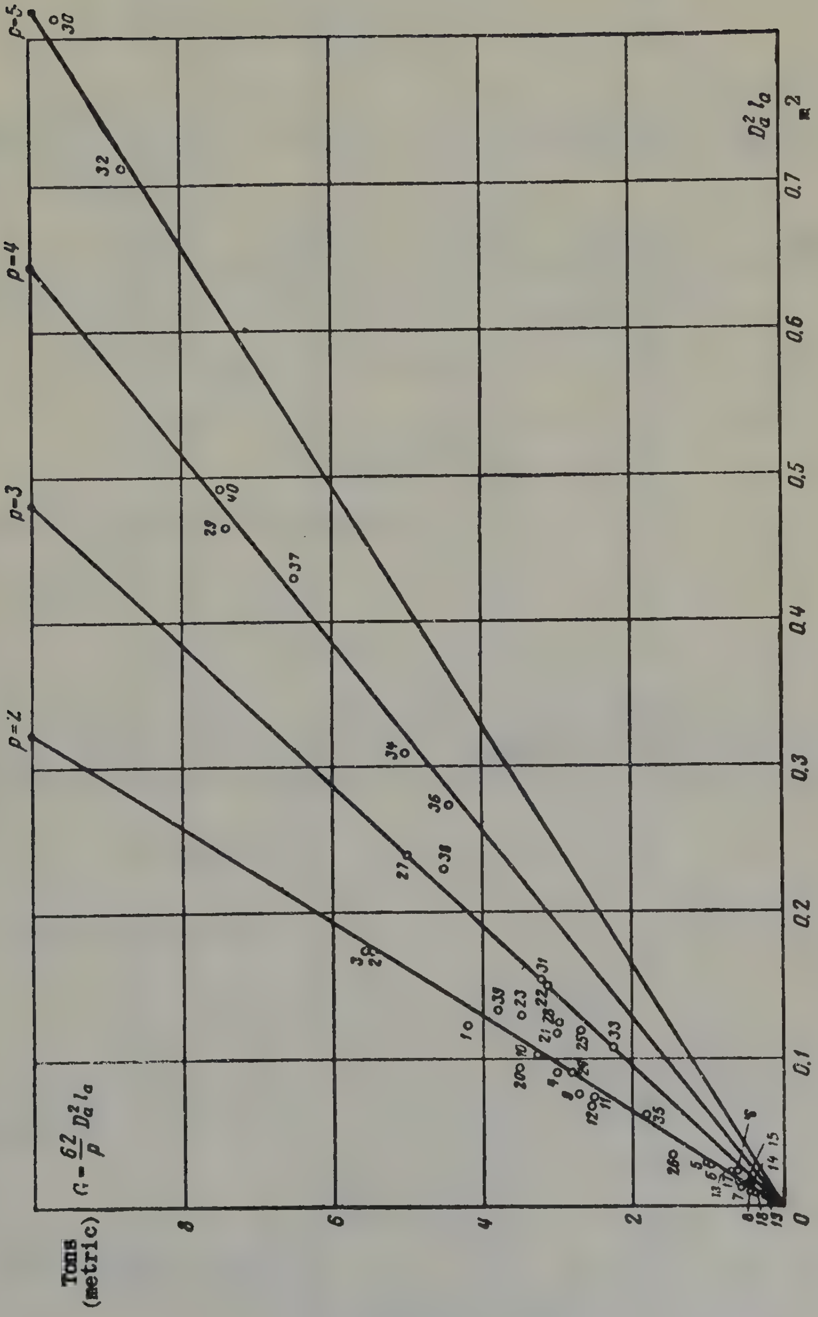
Tons
(metric)
$G = \frac{62}{p} D_a^2 l_a$
p=2
p=3
p=4
p=5
0
2
4
6
8
0
0.1
0.2
0.3
0.4
0.5
0.6
0.7
$D_a^2 l_a$ m^2
30
32
29
40
37
34
36
27
38
3
2
10
39
23
22
31
21
25
33
20
9
8
11
12
35
26
5
6
13
17
7
14
15
16
18
19

Fig. 1

Point	1	2	3	4	5	6	7	8	9	10
Type of machine	DPE-400	NB-406	GE-750	DK-8B	DK-806A	DK-802A	DK-801A	DK-800A	DK-304B	EDT-200A
Point	11	12	13	14	15	16	17	18	19	20
Type of machine	DK-103D	DPI-152	DK-104G	DK-255B	DK-254A	DK-202B	DK-809A	EDR-10	DK-908	SE-752
Point	21	22	23	24	25	26	27	28	29	30
Type of machine	SW-435	TAO-649B-1	TO-1368	EDT-340	TS-32/44	DK-102E	NB-412M	d/TE-60 (project)	MPT 99/47	MPT 120/55
Point	31.	32	33	34	35	36	37	38	39	40
Type of machine	MPT 74/28 T	MPT 120/49	MPT 74/28 T	GP-300	MPT 49-25-3	MPT 84/39	GP-304	GP-305	NB-414	GP-307

p is the number of pole pairs;

D_a – the diameter of the armature, м;

l_a – the length of the armature, м.

Formula (1) gives fairly exact results for traction motors and generators, including old machines as well as the latest foreign and soviet models (Fig. 1).

It follows from (1) that it is expedient to maximize the number of poles in order to reduce weight. But the number of poles is restrieted by the permissible voltage between adjacent brush holders.

If $e_{\mathrm{k.av}} = 20$ V and $D_k/D_a = 0.8$, then

$$p_{\max} = 6.3\frac{D_a}{U_{\max}}, \qquad (2)$$

where $U_{\max}$ is the maximum voltage, kV.

Substituting (2) in (1),

$$G_{\min} \simeq 10 D_a l_a U_{\max} \ [\text{tons}]. \qquad (3)$$

The limit to which it is advisable to reduce the maximum voltage depends on the room for manoeuvre in placing the brush holders. Experience with the construction of single phase commutator motors shows that

$$\tau_{\text{к.min}} = 0.12 \ \text{м}.$$

Using the condition

$$\tau_{a\,\min} = \frac{\pi D_a}{2p_{\max.\mathrm{k}}} = \frac{0.12}{0.8} = 0.15 \ \text{m}.$$

the maximum possible number of pole pairs is limited on constructional grounds to:

$$p_{\max.\text{к}} = 10 D_a. \qquad (4)$$

Substituting (4) in (1),

$$G_{\min.\text{к}} = 6.2 D_a l_a \ [\text{tons}]. \qquad (5)$$

Here the maximum voltage must not exceed the quantity obtained by comparison of (2) and (4), i.e.

$$(U_{\max})_{\min} \leqslant 0.67 \text{ kV}.$$

Calculated outputs of traction machines. The electromagnetic power of a machine is given by the expression:

$$P = \frac{\alpha_i B_\delta A}{6\times10^{11}} D_a^2 l_a n \text{ [kW]}, \tag{6}$$

where D_a and l_a are expressed in cm.

Substituting the expression for speed

$$n = \frac{60 v_a}{\pi D_a} \tag{7}$$

in formula (6):

$$P = \frac{\alpha_i B_\delta A v_a}{\pi \cdot 10^4} D_a l_a, \tag{8}$$

where D_a and l_a are expressed in metres (m).

For non-controlled machines the maximum sustained output can be obtained for the given dimensions of the armature if the appropriate maximum permissible sustained values of the quantities are substituted in (8).

The coefficient of D_a l_a in (8) can be simplified to

$$C_{\text{м}} = \frac{\alpha_{i\,\max} B_{\delta\,\max\,\infty} A_{\max\,\infty} v_{a\,\max\,\infty}}{\pi 10^4} \left[\frac{\text{kW}}{\text{m}^2}\right]. \tag{9}$$

The power defined by the formula

$$P_{\text{р}} = C_{\text{м}} D_a l_a \text{ [kW]},$$

may be known as the *theoretical (volume) power of the machine.*

A wide range of control is typical of traction machines. Thus, for traction generators $U_{\max} > U_\infty$, and for motors $v_{\max} > v_\infty$.

Putting K_r for the coefficient of regulation:

(a) if $n_\infty = n_{max}$ and $v_{a\infty} = v_{a\,max}$ for generators:

$$K_r = \frac{B_{\delta\,max\,\infty}}{B_{\delta\infty}} = \frac{U_{max}}{U_\infty} = K_{Ug}.$$

(b) if μ = const for traction motors

$$K_r = \frac{v_{a\,max\,\infty}}{v_{a\infty}} = \frac{n_{max}}{n_\infty} = \frac{v_{max}}{v_\infty} = K_v$$

The nominal sustained power of the machines is less than the theoretical power by the factor K_r:

$$P_{\infty g} = C_m D_a l_a \frac{U_\infty}{U_{max}}; \tag{10}$$

$$P'_{\infty m} = C_m D_a l_a \frac{V_\infty}{V_{max}}. \tag{10a}$$

As shown above, the dimensions of machines are determined by the theoretical power. In reference to the desired sustained power, the dimensions can be found from 10 and 10(a):

$$D_a l_a = \frac{P_\infty K_p}{C_m} \quad [m^2]. \tag{11}$$

For traction generators

$$D_a l_a = \frac{U_{max} I_\infty}{C_m} \quad [m^2], \tag{12}$$

for traction motors

$$D_a l_a = \frac{F_\infty v_{max}}{367 C_m \eta_m \eta_{per}} \quad [m^2]. \tag{13}$$

The relationship between machine weight and the desired criteria. Substituting (12) and (13) in (1):

$$G_r = 62\,\frac{D_a}{p}\,\frac{U_{max} I_\infty}{C_m} \quad [\text{tons}]; \tag{14}$$

$$G_{\text{м}} = 62\,\frac{D_a}{p}\,\frac{v_{\max} F_{\infty}}{367 C_{\text{м}} \eta_{\text{м}}\,\eta_{\text{per}}}\ [\text{tons}]. \tag{15}$$

The following conclusions can be drawn from (14) and (15):

(a) It is necessary to avoid unwarranted control limits.

(b) It is necessary to make the machines with the maximum possible frequency. Increases in armature diameter should be accompanied by a proportional increase in the number of poles so as to maintain the maximum frequency unchanged.

(c) Maximum peripheral speed is the most "worthwhile" criterion; it must be made as large as possible in each case (an increase in power and efficiency without additional material).

The weight of machines per unit of output. Here it is sufficient to divide (1) by (10) and (10a):

$$\frac{G}{P_{\infty}} = \frac{62}{C_{\text{м}}} \cdot \frac{D_a}{p} \cdot \frac{U_{\max}}{U_{\infty}} \cdot 10^3 \left[\frac{\text{kG}}{\text{kW}}\right]; \tag{14a}$$

$$\frac{G}{P_{\infty\,\text{м}}} = \frac{62}{C_{\text{м}}} \cdot \frac{D_a}{p} \cdot \frac{v_{\max}}{v_{\infty}} \cdot 10^3 \left[\frac{\text{kG}}{\text{kW}}\right]. \tag{15a}$$

Maximum rotational speed of a machine. For generators:

$$n_{\infty} = n_{\max} = \frac{60 v_{a\,\max}}{\pi D_a}. \tag{16}$$

for motors

$$n_{\infty} = \frac{n_{\max}}{K_v} = \frac{60 v_{a\,\max}}{\pi D_a} \cdot \frac{v_{\infty}}{v_{\max}}. \tag{17}$$

The optimum transmission ratio of traction motors:

$$\mu_{\text{opt}} = 3.6\,\frac{D_0 v_{a\,\max}}{D_a v_{\max}}. \tag{18}$$

If $v_{a\,\max} = 70$ m/sec and $D_0 = 1.05$ m.

$$\mu_{\text{opt}} = \frac{265}{D_a v_{\max}}.$$

The most "efficient" frequency of a machine. It will be seen from (14a) and (15a) that the weight per unit of output is proportional to the ratio D_a/p. By (7) and the expression for frequency

$$f=\frac{pn}{60} \tag{19}$$

this ratio equals

$$\frac{D_a}{p}=\frac{v_a}{\pi f}\,. \tag{20}$$

Substituting (14a) and (15a) in expressions (9) and (20):

(a) for a generator

$$\frac{G}{P_{\infty g}}=\frac{62\cdot 10^9}{\alpha_i B_\delta A f}\cdot\frac{U_{\max}}{U_\infty}\left[\frac{\mathrm{kG}}{\mathrm{kW}}\right]; \tag{21}$$

(b) for a motor

$$\frac{G}{P_{\infty m}}=\frac{62\cdot 10^9}{\alpha_i B_\delta A f}\cdot\frac{v_{\max}}{v_\infty}\left[\frac{\mathrm{kG}}{\mathrm{kW}}\right]. \tag{22}$$

The greatest frequency is the most "efficient" frequency from the point of view of weight indices.

Refinement of the formulae in Table 1 for the criterion of commutation. The practicability of electric machines can be checked in the first instance by V.T. Kas'ianov's criteria for non-controlled generators:

(a) the permissible inter-lamella voltage (that between the commutator segments):

$$P_\infty\leqslant(1\,400 \text{ to } 1\,700)\,D_a\ [\ \mathrm{kW}\ ]; \tag{23}$$

(b) the permissible values of v_a and A

$$P\leqslant\frac{1.2 \text{ to } 1.8}{n}\cdot 10^6\ [\ \mathrm{kW}\ ]; \tag{24}$$

TABLE 1

Fundamental formulae and coefficients

Formula and coefficients	1st version	2nd version
α_i	0.67	0.7
B_δ	10^4	$1.06\cdot10^4$
A	500	600
v_a	70 m/sec	700 m/sec
$C_{\text{м}}$	7 500 kW/m^2	10 000 kW/m^2
$P_{\infty m}$	$7\,500 D_a l_a \frac{U_\infty}{U_{\max}}$ [kW]	$10\,000 D_a l_a \frac{v_\infty}{U_{\max}}$ [kW]
$P_{\infty g}$	$7\,500 D_a l_a \frac{v_\infty}{u_{\max}}$	$10\,000 D_a l_a \frac{v_\infty}{U_{\max}}$ [kW]
$(D_a l_a)_g$	$\frac{U_{\max} I_\infty}{7\,500}$ [m^2]	$\frac{U_{\max} I_\infty}{10\,000}$ [m^2]
$(D_a l_a)_m$	$4F_\infty v_{\max}\cdot10^{-4}$ [m^2]	$3F_\infty v_{\max}\cdot10^{-4}$ [m^2]
$P_{\max.\text{к}}$	$10D_a$	$10D_a$
$U_{\max(opt}$	0.63 kV	0.63 kV
$I_{\max(opt)}$	$220\cdot\frac{0.63}{U_{\max}}$	$220\cdot\frac{0\ 63}{U_{\max}}$
$G_{\min.\text{к}}$	$6.2 D_a l_a$ [tons]	$6.2 D_a l_a$ [tons]
$\left(\frac{G}{P_\infty}\right)_{\min.\text{к}}$	$0.83K_r$ [kg/kW]	$0.62K_r$ [kg/kW]
$\left(\frac{G}{P_\infty}\right)_{\min}$	$1.24K_rU_{\max}$	$0.98K_rU_{\max}$

(c) the intensity of commutation:

$$D_a l_a < \frac{250 \text{ to } 450}{n} \ [\mathrm{m}^2]. \tag{25}$$

Criteria (23) and (24) are taken into account in finding the value of C_m and $p_{\max}$. Criteria (25) restricts the length of the machine armature depending on the conditions of commutation.

Substituting in (25) the expression for speed

$$n = \frac{60 v_a}{\pi D_a} = \frac{60 \cdot 70}{\pi D_a} = \frac{4\,200}{\pi D_a};$$

then by definition

$$l_a \leqslant \frac{240 \text{ to } 450}{4\,200} = 0.18 \text{ to } 0.336 \ \mathrm{m} \tag{26}$$

This criterion will now be reconsidered on the basis of one of the latest traction generators which is made by the Elektrotiozhmash traction works and has quite satisfactory commutation (GP-304 type machine, $P = 1\,350$ kW, $D_a = 0.99$, $l_a = 0.44$, $A = 505$, $n = 850$ rev/min). In this case

$$0.44 \leqslant \frac{240 \text{ to } 450}{0.99 \cdot 850} = 0.290 \text{ to } 0.535 \ \mathrm{m}.$$

Thus, the criterion is only satisfied for the upper limit with good commutation.

In the following it is proposed to take typical characteristics of this generator as the criteria of commutations:

$$D_a l_a n = 0.99 \cdot 0.44 \cdot 850 = 370. \tag{27}$$

If $v_{a\,\max} = 70$ m/sec, the following quantity corresponds to this criterion:

$$l_a = \frac{370\pi}{4\,200} = 0.28 \ \mathrm{m}.$$

Another still more general criterion is

$$A l_a v_a \approx 10^4 = \text{const}. \tag{28}$$

The formula for traction generators in the light of the limitations imposed by (28) has the form:

$$P_{\infty}K_{Ug}=D_a\frac{C_M\cdot 10^4}{Av_{a\,\max}}\ [\mathrm{kW}]. \tag{29}$$

If $v_{a\,\max}=70$ m/sec, $A=500$ and $C_M=7500$, then

$$P_{\infty}K_{Ug}=\frac{7\,500\cdot 10^4}{500\cdot 70}=2\,140D_a\ [\mathrm{kW}]; \tag{29a}$$

and if $v_{a\,\max}=70$ m/sec, $A=600$ and $C_M=10^4$,

$$P_{\infty}K_{Ug}=\frac{10^4\cdot 10^4}{600\cdot 70}=2\,375D_a\ [\mathrm{kW}].$$

Including the special features of traction motors, these formulae take the form

$$P_{\infty}K_v=D_a\frac{C_M\cdot 10^4}{\frac{A}{K_{Ug}}v_{a\,\max}}\ [\mathrm{kW}],$$

or

$$P_{\infty}=D_a\frac{C_M\cdot 10^4}{Av_{a\,\max}}\cdot\frac{K_{Ug}}{K_v}\ [\mathrm{kW}]. \tag{30}$$

If $v_{a\,\max}=70$ m/sec, $A=500$ and $C_M=7500$:

$$P_{\infty}=D_a\frac{7\,500\cdot 10^4}{500\cdot 70}\cdot\frac{K_{Ug}}{K_v}=2\,140D_a\frac{K_{Ug}}{K_v}; \tag{30a}$$

or if $v_{a\,\max}=70$ m/sec, $A=600$ and $C_M=10^4$:

$$P_{\infty}=\frac{10^4\cdot 10^4}{600\cdot 70}D_a\frac{K_{Ug}}{K_v}=2\,375D_a\frac{K_{Ug}}{K_v}. \tag{30b}$$

Formulae 30(a) and 30(b) provide (1) the limiting outputs of traction motors in reference to the diameter of the wheel and the gauge of the line and (2) the necessary coefficient of voltage regulation as a function of the desired range of speed control.

Thus, for example, if $D_c = 1.05$ m, $D_{a\,\max} = 0.49$ m and $A = 500$ then

$$P_\infty = 2\,140 \cdot 0.49 \cdot \frac{K_{Ug}}{K_v} = 1\,050\,\frac{K_{Ug}}{K_v}; \qquad (31)$$

and if $D_0 = 1.05$ m, $D_a = 0.49$ m and $A = 600$, then

$$P_\infty = 2\,375 \cdot 0.49\,\frac{K_{vg}}{K_v} = 1\,160\,\frac{K_{Ug}}{K_v}. \qquad (32)$$

On the other hand, since

$$P_\infty K_v = (7\,500 \text{ to } 10\,000) D_a l_a;\; P_\infty = \frac{7\,500 \text{ to } 10\,000}{K_v} D_a l_a,$$

it follows that if $D_a = 0.49$ m, then

$$P_\infty = (3\,750 \text{ to } 5\,000)\,\frac{l_a}{K_v}. \qquad (33)$$

From a comparison of (31)-(33):

$$\frac{K_{Ug}}{l_a} = \frac{3\,750 \text{ to } 5\,000}{1\,050 \text{ to } 1\,160} = 3.5 \text{ to } 4. \qquad (34)$$

Consequently, there is a constant relationship between K_{Ug} and

For line gauges of 1524 mm and $l_{a\,\max}$ equal to 0.5 m,

$$K_{Ug} = (3.5 \text{ to } 4.3) \cdot 0.5 = 1.75 \text{ to } 2.15;$$

and for line gauges of 1435 mm and $l_{a\,\max}$ equal to 0.43 m:

$$K_{Ug.\max} = (3.5 \text{ to } 4.3) \cdot 0.43 = 1.5 \text{ to } 1.85.$$

The limiting output of a traction motor for a wheel diameter $D_0 = 1.05$ m ($D_a = 0.49$ m) and $A = 600$ is (see Table 2):

$$P_\infty = 1\,160 \cdot 2.15\,\frac{1}{K_v} = \frac{2\,500}{K_v}. \qquad (35)$$

This output can be attained if $K_{Ug} = 2.15$.

TABLE 2

K_v	2	2.5	3	4	5
P_∞, kW	1 250	1 000	835	625	500

Conclusions

The weight of the electrical machines of Diesel locomotives and gas turbine locomotives and that of the traction motors of electric single phase d.c. locomotives may be reduced considerably in individual cases by a factor of 1.5 to 2 using conventional materials.

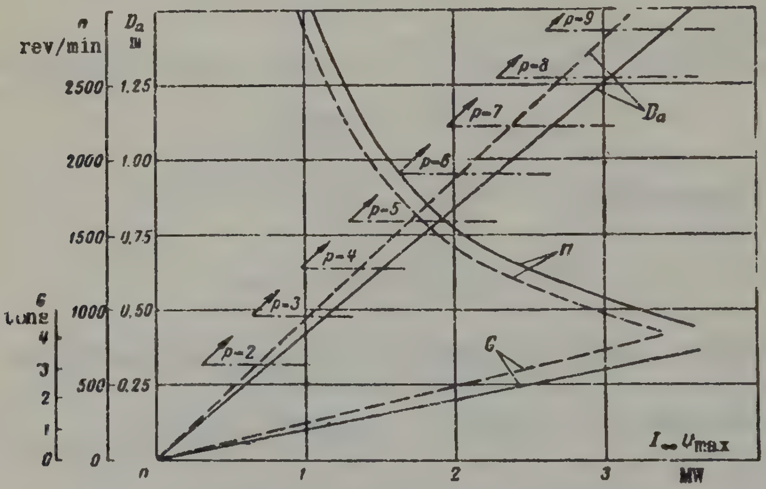

Fig. 2. Nomogram of the optimum calculated data for traction generators:
U_{max} = 1kV; $Av_a l_a = 10^4$; ----- C_m = 7500; l_a = 0.285 m; A = 500; $B\delta$ = 0.75 x 10^4; v_a = 70; a_i = 0.67; ——— - $C_m = 10^4$, l_a = 0.238 m; A = 600; B_δ = = 1.66 x 10^4; v_a = 70; a_i = 0.7.

All traction motors can be "inscribed" in the diameter of a wheel D_r = 1.05 m. When making traction motors with the limiting length of armature (l_a = 0.5 m), the range of voltage control of the generator should reach K_{Ug} = 2.15.

The weight of the generators must not exceed 1.5 U_{max}/U_∞ kg/kW, and that of the motors should not exceed 1.5 v_{max}/v_∞ kg/kW. The gross weight of the main machines of Diesel locomotive transmissions should not exceed

$$1.5\left(\frac{U_{\max}}{U_{\infty}}+\frac{v_{\max}}{v_{\infty}}\right)\left[\frac{\text{kG}}{\text{kW}}\right].$$

The weight of traction motors is proportional to $F_{\infty}\, v_{\max}$ and this should therefore be kept as low as possible.

In traction machines it is recommended that nomograms should be used. Those shown in Fig. 2 refer to generators and those in Fig. 3 to motors

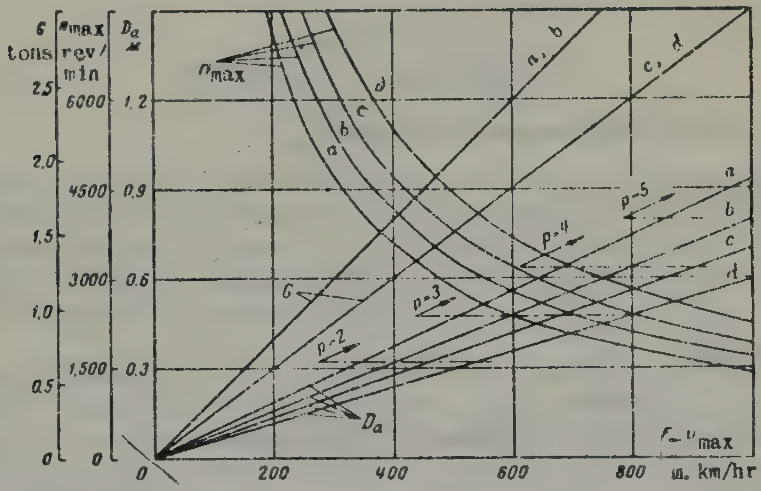

Fig. 3. Nomogram of the calculated data for traction motors:

a – $C_{м} = 7500$, $l_a = 0.43$, $K_{Ug} = 1.5$; b – $C_{м} = 7500$, $l_a = 0.5$, K_{Ug} 1.75; c – $C_{м} = 10^4$, $l_a = 0.43$, $K_{Ug} = 1.85$, d – $C_{м}$ 10^4, $l_a = 0.5$, $K_{Ug} = 2.15$.

An important condition for obtaining the requisite weight indices is the production of electrical machines with the maximum number of poles:

$$p_{\max} = 6.3\,\frac{D_a}{U_{\max}}\left[\frac{\text{m}}{\text{kV}}\right]$$

or, what is the same thing, with the maximum frequency

$$f_{\max} = \frac{140}{U_{\max}}\ [\text{c/s}].$$

Consequently, the weight of a machine is proportional to the maximum voltage and for low power machines it is necessary to take $U_{\max} < 0.67$ kV, and for high powered machines $U_{\max} \leqslant 1$ kV.

The calculations have shown that in principle it is possible to produce traction machines with a weight of 1.5 kg/kW.

The calculations which have been performed for generators in sustained use show that (1) produces a considerable error if the length of the armature is short or if the number of poles is large. It is therefore recommended that a more general formula be used:

$$G = D_a^2 \left(\frac{p}{30 l_a} + \frac{60 l_a}{p} \right) [\text{tons}]. \tag{1a}$$

The following formulae may also be used:

for generators:

$$G = 1.2 + \frac{60}{p} D_a^2 l_a \; [\text{tons}]; \tag{1b}$$

for motors:

$$G = 0.3 + \frac{60}{p} D_a^2 l_a \; [\text{tons}]. \tag{1c}$$

In order to produce generators of low weight, these machines should not be made with armature diameters over 0.85 m, since otherwise the weight of their frames must be increased in order to ensure adequate rigidity. If it is necessary to produce large outputs, it is expedient to mount two short armatures in one housing.

Translated by O.M. Blunn

OPERATION OF AN INDUCTION MOTOR FROM A TRANSISTOR FREQUENCY CONVERTER*

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(Moscow)

(Received 9 May 1960)

The most economical method of regulating the speed of a squirrel cage induction motor is to vary the frequency of the supply source In this connexion special importance is attached to static variable-frequency d.c. to a.c. converters in which large junction-type transistors are used as switching devices.

The most efficient mode of operation of a transistor is the switching state which permits highly efficient energy conversion. Yet the output voltage of a simple transistor converter used as a switch is rectangular in shape (see Fig. 1a). It is to be expected that the operating characteristics of an induction motor will therefore deteriorate owing to the presence of higher harmonics [1].

In order to assess the merits of simple transistor frequency converters in this case, it is necessary to compare the operating characteristics of the motor when a voltage of sinusoidal shape is supplied with those obtaining when the supply voltage is of rectangular shape. Such a comparison shows just how far it is advisable to rely on complex semiconductor converters based on switching triodes for output voltage curves of approximately sinusoidal shape [2].

Two typical cases of a rectangular voltage supply are considered:

(a) a rectangular voltage containing all odd harmonics (Fig. 1a);

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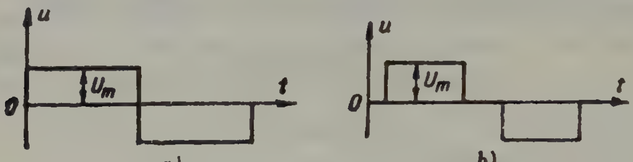
* *Elektrichestvo*, No. 9, 29-36, 1961.

(b) a rectangular voltage containing all odd harmonics except multiples of three (Fig. 1, *b*).

The circuit of a frequency converter is shown in Fig. 2 [3]. The shape of the output voltage curve corresponds to that in Fig. 1(a) or (b) depending on the method of connecting the load.



a) b)

Fig. 1. Curves for the converter output voltages.

The circuit contains three single-phase converters, each of which consists of a transformer with a rectangular hysteresis loop, connected in the diagonal of a bridge of four power triodes. The single phase converters operate in the same way as in Royer's circuit [4], except that the transformer is not a power transformer; it is intended for controlling the triodes. In one half-wave the triodes T_{11} and T_{14} are in the state of saturation and T_{12} and T_{13} in the state of cut-off. In the next half-wave the position is reversed.

All three converters are synchronized with the angle of phase shift of the output voltage (equal to $2\pi/3$). The converter FC_1 is the driving one in relation to FC_2, and FC_2 is the driving one in relation to FC_3. The pair of triodes T_{21}, T_{23} (T_{31}, T_{33}) of each driven converter are controlled by two parallel-connected windings, viz. one from the transformer proper w_{21}, w_{23} (w_{31}, w_{33}), and the other from the transformer of the driving converter w_{c11}, w_{c12} (w_{c21}, w_{c22}). A choke (reactor) Ch_1 (Ch_2) is connected in series with the windings of the driving converter. The voltage on the synchronizing windings w_{c11}, w_{c12} (w_{c21}, x w_{c22}) is greater than that on the windings of the transformer proper. Therefore, the windings w_{c11}, w_{c12}, (w_{c21}, w_{c22}) switch the triodes T_{23}, T_{21} (T_{33}, T_{31}) of the driven converter on saturation of the synchronizing choke Ch_1 (Ch_2). The state of the triodes of the driven converter is maintained until the next switching by the windings of the transformer proper.

The angle of saturation of the choke is:

$$\gamma = \frac{4w_{ch}\Phi_{s\,ch}}{\frac{w_{c11}}{w'_1} U \cdot 10^8}, \tag{1}$$

where w_{ch} is the number of turns of the choke, $\Phi_{s\ ch}$ the saturation flux of the choke and U the supply voltage.

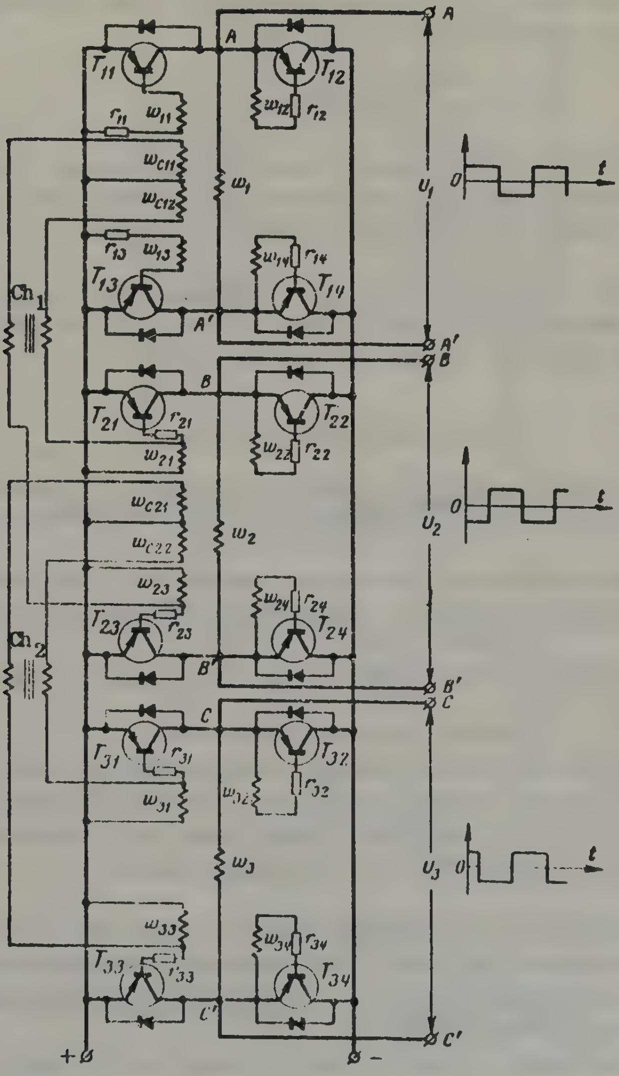


Fig. 2. Circuit of frequency converter.

In order that the angle between the voltages of the converters FC_1 FC_2 should remain constant throughout the entire range of frequency variation, it is necessary that the ratio of γ to the half-period T should remain constant and equal to 2/3. Here T is found from the formula:

$$T = \frac{4w_1\Phi_{s1}}{U \cdot 10^8}, \tag{2}$$

where Φ_{s1} is the saturation flux of the transformer.

In fact, the ratio γ / T is independent of U, and the parameters of the chokes are found from the condition:

$$\frac{w_{ch}\Phi_{s\,ch}}{w_{c11}\Phi_{s1}} = \frac{2}{3}. \tag{3}$$

With a variation of the voltage, the saturation time of the cores and the half-period both vary in proportion, so that the angle between the output voltages of the converters remains constant.

An important condition of involuntary synchronization is that the fundamental (natural) frequency of the driven converter must be 8 to 10 per cent less than that of the driving converter, i.e.

$$f_{fc3} = f_{fc2} < f_{fc1} = f, \tag{4}$$

where f_{Fc3}, f_{Fc2}, f_{Fc1} are the fundamental (natural) frequencies of the converters and f the desired frequency of the three phase converter.

Otherwise the triodes of the driven converter are switched before the time for involuntary switching, and synchronization is disturbed.

All the triodes in this converter are fully used as regards voltage and current.

The circuit in Fig. 2 is suitable for regulating the speed of an induction motor between limits of 1 to 3 for a constant torque and between limits of 1 to 10 for a ventilator load, since the voltage across the base windings is proportional to the supply voltage.

If the amplitude of the output voltage is less than the permissible collector voltage of the triode, the load can be connected (a) in the diagonal of each bridge A-A', B-B', C-C', or (b) at the output of two individual three phase bridges.

But if the output voltage is greater than the permissible collector voltage of the triode, it is then possible (a) to design the transformers

Tr_1, Tr_2, Tr_3 for the total output power and to connect the load to the output windings of the transformers Tr_1, Tr_2, Tr_3 or (b) design these three transformers for the control power and connect the primary windings of the power transformers in the diagonal of each bridge A-A', B-B', C-C' or at the output of the individual three phase bridges.

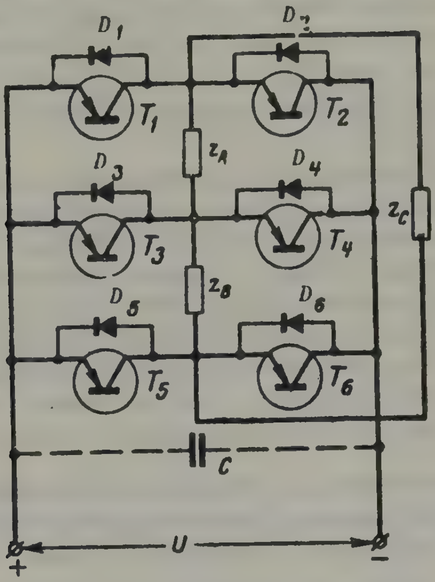


Fig. 3. Circuit of the power part of a converter with load delta-connected

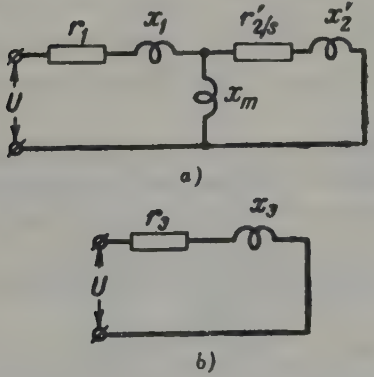


Fig. 4. Equivalent circuit of induction motor.

But if the output voltage is greater than the permissible collector voltage of the triode, it is then possible (a) to design the transformers Tr_1, Tr_2, Tr_3 for the total output power and to connect the load to the output windings of the transformers Tr_1, Tr_2, Tr_3 or (b) design these three transformers for the control power and connect the primary windings of the power transformers in the diagonal of each bridge A-A', B-B', C-C' or at the output of the individual three phase bridges.

In view of the losses in the transformer, it is advisable to use a transformerless output and at high voltages to use series-connected triodes [5]. In the first case the output voltage will contain all odd harmonics (Fig. 1a), whereas in the other case the output voltage contains no harmonics which are multiples of three (Fig. 1b).

The best characteristics are possessed by an autonomous inverter [6] consisting of a three phase bridge type amplifier (see Fig. 3) and a three phase main generator. The latter consists of a variable frequency multivibrator and two conventional converters synchronized with the first by magnetizable saturable reactor. The frequency of the main generator is regulated by d.c. signals independently of the voltage across the power triodes. The variation of the voltage across the power triodes does not affect the frequency of the converter. The output voltage of this converter has the same shape as that shown in Fig. 1(b).

Tests were carried out on the system illustrated in Fig. 2. The power triodes of the semiconductor converter were shunted by diodes owing to the phase shift between the current and voltage on inductive-active load.

The following assumptions were made:

(a) The saturation of the motor was neglected;

(b) The control pulses were assumed to be rectangular in shape;

(c) The characteristics of the triodes and diodes were assumed to be ideal;

(d) The switching time of the triodes was assumed to be zero.

Consider first the operating characteristics of an induction motor with an input voltage of rectangular shape which generally contains all odd harmonics. The curve in Fig. 1(a) is expanded into a Fourier series:

$$U = \frac{4U_m}{\pi}\left[\sum_{n=0}^{n=\infty}\frac{\sin(6n+1)\omega t}{6n+1} + \sum_{n=1}^{n=\infty}\frac{\sin(6n-1)\omega t}{6n-1} + \sum_{n=1}^{n=\infty}\frac{\sin 3(2n-1)\omega t}{3(2n-1)}\right], \tag{5}$$

where the harmonics of positive, negative and zero sequences are in separate sums.

Proceeding from the equivalent circuit of an induction motor (Fig. 4a), the *n*th order harmonic of the stator current is found from the expression

$$\dot{I}_{1n} = \frac{u}{n} \cdot \frac{a_1 + n^2 r'_2 s_n + a_2 n^2 c_2 s_n^2 - j(n^3 a_3 c_2 s_n^2 + n a_4)}{a_1 r_1 + n^2 a_5^2 + n^2 a_6 s_n + n^4 a_3^2 s_n^2 + n^2 a_2^2 s_n^2}, \qquad (6)$$

where

$$\left.\begin{aligned} a_1 &= \frac{r_1 r_2'^2}{x_m^2}; \quad a_2 = r_1 c_2; \quad a_3 = (x_1 c_2 + x'_2); \\ a_4 &= \frac{r_2'^2 c_1}{x_m}; \quad a_5 = r'_2 c_1; \quad a_6 = 2 r_1 r'_2; \\ a_7 &= \frac{r_1 r'_2}{x_m}; \quad c_1 = \left(1 + \frac{x_1}{x_m}\right); \quad c_2 = \left(1 + \frac{x_3}{x_m}\right). \end{aligned}\right\} \qquad (7)$$

The rotor slip in the field of the *n*th harmonic is:

$$s_n = \frac{n' n_1 \mp n_2}{n n_1}, \qquad (8)$$

where n_1 is the speed of the stator field (for the first harmonic) and n_2 the rotor speed.

In (8) the "minus" sign is then taken for the positive sequence harmonics and the "plus" sign for the negative sequence harmonics so that (8) takes the form

$$s_n = \frac{(n \mp 1) \pm s_1}{n}. \qquad (8a)$$

The *n*th order harmonic of the rotor current, referred to the stator winding, is found by the formula

$$\dot{I}_{2n} = \frac{u}{n} \cdot \frac{n^2 a_2 s_n^2 + n^2 a_5 s_n - j(n^2 a_3 s_n^2 - n a_7 s_n)}{a_1 r_1 + n^2 a_5^2 + n^2 a_6 s_n + a_3^2 s_n^2 n^4 + a_2^2 s_n^2 n^2}. \qquad (9)$$

The power factor for *n*th order harmonic is

$$\cos \varphi_n = \frac{1}{\sqrt{1 + \left(\dfrac{n^3 a_3 c_2 s_n^2 + n a_4}{a_1 + n^2 r'_2 s_n + n^2 a_2 c_2 s_n^2}\right)^2}}. \qquad (10)$$

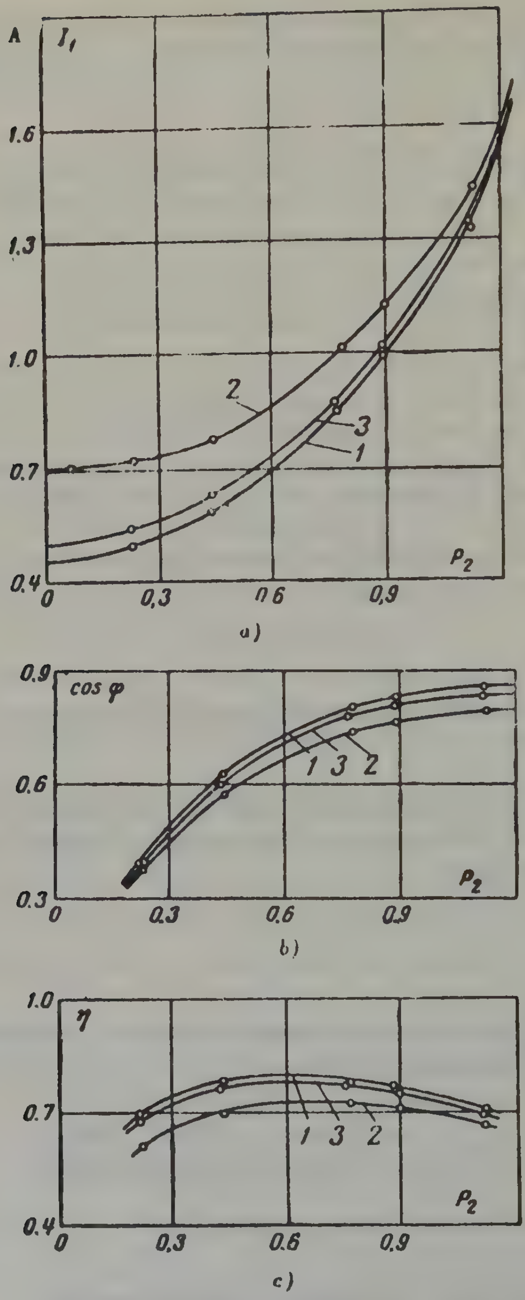


Fig. 5. Operating characteristics of induction motor. *1* – sinusoidal voltage; *2* – rectangular voltage with all odd harmonics; *3* – stepped voltage without harmonic multiples of 3; P_2 – relative power on shaft.

The resulting current and torque of the motor are then

$$I_1 = \sqrt{\sum_{1}^{n=\infty} I_{1n}^2}; \quad M = \sum_{1}^{n=\infty} M_n. \tag{11}$$

The operating characteristics of an induction motor have been plotted in Fig. 5 by (6)-(11). The motor parameters were as follows:

$$r_1 = 2.2 \ \Omega; \quad r_2' = 2.28 \ \Omega; \quad z_m = 46 \ \Omega;$$

$$x_1 = 2 \ \Omega; \quad x_2' = 3 \ \Omega.$$

Curves 1 in Fig. 5 refer to a sinusoidal supply voltage of 22.5 V; curves 2 were obtained for a supply voltage of rectangular shape containing all odd harmonics (the fundamental harmonic equalled the sinusoidal voltage 22.5 V). In both cases the supply frequency was 50 c/s.

Here the ratios of the mean and effective values of the sinusoidal and rectangular voltages respectively were:

$$\left.\begin{aligned} \frac{U_{\mathrm{s.av}}}{U_{\mathrm{s.eff}}} &= \frac{8}{\pi^2}; \\ \frac{U_{\mathrm{s.av}}}{U_{\mathrm{r.eff}}} &= \frac{2\sqrt{2}}{\pi}, \end{aligned}\right\} \tag{12}$$

where $U_{\mathrm{s.av}}$ and $U_{\mathrm{r.av}}$ are the mean values of the sinusoidal and rectangular voltages.

Calculations have shown that the decisive moment on the supply of a rectangular voltage is that created by the fundamental harmonic since the moments of higher harmonics are negligible. The higher harmonics cause an increase in the motor current, a deterioration in cos φ and an increase in losses so that the efficiency of the motor is reduced (see curves 2 in Fig. 5). For example, under rated conditions the motor current is increased by 10 per cent, cos φ is reduced by 8 per cent and efficiency is reduced by 5 to 7 per cent.

The deterioration in the characteristics of the motor is mainly due to the third harmonic of the rectangular voltage.

Accordingly, it is to be concluded that the supply of an induction motor with a voltage of rectangular shape which contains all odd harmonics is inefficient owing to the deterioration in its operating characteristics.

Consider now a typical case when the shape of the output voltage is like that shown in Fig. 1, (b). On expanding this curve into a Fourier series:

$$u = \frac{2\sqrt{3}}{\pi} U_m \left[\sum_{0}^{n=\infty} \frac{\sin(6n+1)\omega t}{6n+1} + \right.$$
$$\left. + \sum_{n=1}^{n=\infty} \frac{\sin(6n-1)\omega t}{6n-1} \right]. \qquad (13)$$

Curves 3 in Fig. 5 have been obtained for the same motor with a supply voltage of the shape shown in Fig. 1, (b) on condition that the fundamental harmonics of the rectangular and sinusoidal voltages were both the same. This condition is satisfied if

$$U_{s.av} = \frac{6\sqrt{3}}{\pi^2} U_{r.av} = 1.05 U_{r.av}$$

or (14)

$$U_{s.eff} = \frac{3}{\pi} U_{r.eff} = 0.955 U_{r.eff}$$

It will be seen from a comparison of curves 1 (sinusoidal voltage) and curves 3 (rectangular voltage without harmonic multiples of 3) that the operating characteristics of the motor are not appreciably affected in the latter case. Thus, for example, the power factor cos φ is only reduced by 2 or 3 per cent, whilst the efficiency is only reduced by 1 to 1.5 per cent. The motor current is notably increased on no load; the difference between the currents decreases with increasing load. The explanation is that there is practically no variation of the rotor slip in the field of higher harmonics with changes in slip in the field of the fundamental harmonic within the limits 0-1. Thus, for example, for the fifth harmonic $s_5 = 1.2$ to 1, and for the seventh harmonic $s_7 = 0.857$ to 1. Currents of higher harmonics therefore remain constant when the load changes. It is obvious a lighter load means that currents of higher harmonics will have a greater influence. At rated load the current increases by 2 to 3 per cent and the motor torque remains practically unchanged.

In order to analyse the switching of power triodes which are shunted by diodes, it is sufficient to consider the current in two phases, for example, *A* and *B*, and the operation of one pair of triodes: T_3, T_4 (Fig. 3). Suppose that the triodes T_1, T_3 and T_6 are in the cut-off state at the instant $t = 0$ (Fig. 6) and that triodes T_2, T_4 and T_5 are saturated. The operating point for T_3 is B_6, and that for T_4-A_1. In the interval $t_0 \leqslant t \leqslant t_1$ the current in phase B increases according to the law:

$$i_1(t) = \frac{U_m}{r_k} - \frac{U_m}{r_k} \cdot \frac{1+e^{-\frac{1}{6f\tau}}}{1+e^{-\frac{1}{2f\tau}}}\, e^{-\frac{t}{\tau}}, \tag{15}$$

where

$$\tau = \frac{x_k}{\omega r_k},$$

f is the mains frequency, and the resistance r_k and inductive reactance x_k of the motor are respectively

$$r_k = r_1 + \frac{x_m^2 \frac{r_2'}{s}}{\left(\frac{r_2'}{s}\right)^2 + (x_m + x_2)^2}$$

$$x_k = x_1 + x_m \frac{x_2'^2 + x_m x_2' + \left(\frac{r_2'}{s}\right)^2}{\left(\frac{r_2'}{s}\right)^2 + (x_m + x_2)^2}$$

In phase A the current decreases according to the law:

$$i_2(t) = \frac{U_m}{r_k} \cdot \frac{1-e^{-\frac{1}{3f\tau}}}{1+e^{-\frac{1}{2f\tau}}}\, e^{-\frac{t}{\tau}}. \tag{16}$$

A line current i_l flows through the triode T_4, i.e. the sum of the currents in phases A and B is:

$$i_1(t) - i_2(t) = i_{l1} = \frac{U_m}{r_k} - \frac{U_m}{r_k} \times \frac{\left(1+e^{-\frac{1}{6f\tau}}\right)\left(2-e^{-\frac{1}{6f\tau}}\right)}{1+e^{-\frac{1}{2f\tau}}}\, e^{-\frac{t}{\tau}} \tag{17}$$

The operating point is moved from A_1 to A_2. At the instant $t = t_1$, the triodes T_1 and T_2 are respectively switched into the saturation and cut-off states. In the interval $t_1 \leqslant t \leqslant t_2$ the current in phase A begins to increase in the reverse direction in accordance with (15); in phase B the current continues to increase in accordance with (15). A second

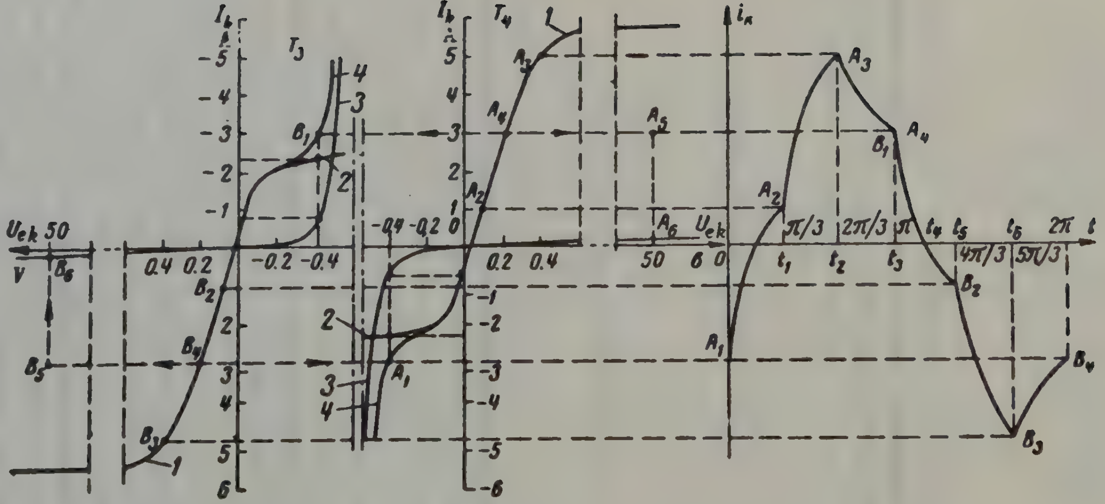


Fig. 6. Commutation of the load current in semiconductor converter. 1 - $I_k = f\ (U_{ek})$ for positive polarity of U_{ek}; 2 - the same except negative polarity; 3 - characteristic of diode in conducting direction; 4 - overall characteristic of triode (with negative polarity U_{ek}) and diode in conducting direction.

line current flows through the triode T_4 equal to:

$$i_1(t)+i_1(t+t_1)=i_{l2}=\frac{2U_m}{r_k}-\frac{U_m}{r_k}\times\frac{\left(1+2e^{-\frac{1}{6f\tau}}+e^{-\frac{1}{3f\tau}}\right)}{1+e^{-\frac{1}{2f\tau}}}e^{-\frac{t}{\tau}}.$$

The operating point of T_4 is now moved from A_2 to A_3.

At the instant $t = t_2$ the triodes T_6 and T_5 are switched respectively into the saturation and cut-off states. In the interval $t_2 \leqslant t \leqslant t_3$ the current in phase A continues to increase in accordance with (15), and that in phase B coincides in accordance with (16). A current then flows through the triode T_4 equal to:

$$i_1(t+t_1)+i_2(t)=i_{l3}=\frac{U_m}{r_k}+\frac{U_m}{r_k}\times\frac{1-e^{-\frac{1}{6f\tau}}-2e^{-\frac{1}{3f\tau}}}{1+e^{-\frac{1}{2f\tau}}}e^{-\frac{t}{\tau}}. \qquad (18)$$

The operating point T_4 is moved from A_3 to A_4. At the instant $t = t_3$ the process of switching triodes T_3 and T_4 begins. As soon as triode T_4 becomes "conducting" under the action of the self-induction e.m.f. of the windings of phases A and B, its operating point moves instantaneously to the point A_5, whilst that of T_3 moves to O. In the course of transient behaviour the current of triode T_4 decreases, whilst that of T_3 increases so that at any instant in time

$$i_{l3}=i_{T3}+i_{T4}. \qquad (19)$$

The operating point T_4 is moved from A_5 to A_6 and that of T_3 for O to B_1. Thus, triode T_4 is in the zone of cut-off, and T_3 in that of saturation. In the interval $t_3 \leqslant t \leqslant t_5$ phase A is disconnected from the supply source and short-circuited by the triode T_1 and the parallel circuit D_3-T_3. The current which is maintained by the self-induction e.m.f. of the motor winding flows through the triode T_1 in phase A in the former direction; the current through diode D_3 flows in the conduction direction; that through T_3 is in the reverse direction (i.e. in the direction of the collector-emitter).

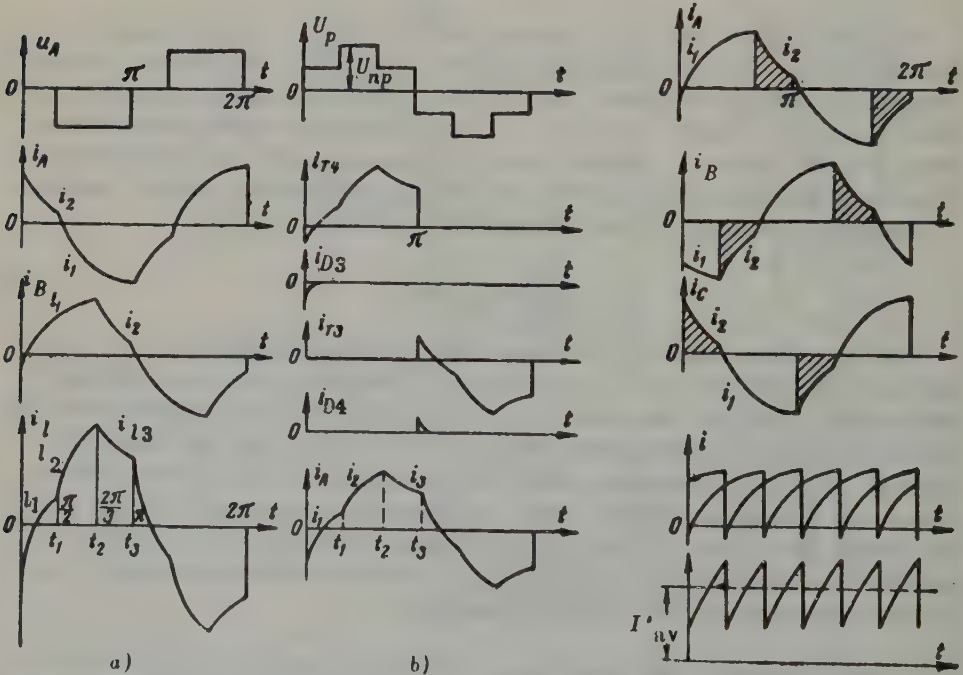
It will be seen from Fig. 6 that the current amplification factor of a P4-type triode (the ratio of the base current to the saturation current of the collector) is 2.5 times less in the reverse direction than in the forward direction. Thus, for example, if the operating point of the triode in the forward direction at 5 A is in the saturation zone for a definite base current, then for the same base current in the opposite direction the operating point will depart into the active region even at currents above 2 A and even in the absence of a shunting diode. The connexion of a shunting diode ensures the operation of the triode with reverse currents in the region of saturation.

The current in phase *B* at the first instant after switching remains in the same direction owing to the self-induction e.m.f. Two cases are possible:

1. If $1 \geqslant \cos\varphi \geqslant 0.525$ the current of phase *C* and the triodes T_1 and T_3, thereby reducing the current consumed from the d.c. circuit so that at any instant in time

$$i'(t)+i_B(t)=i_C(t). \tag{20}$$

In other words, the current of phase *B* is completely compensated by the current of phase *C*.



a) b) c)

Fig. 7. *a* – voltage across phase *A*, currents in *A* and *B* and line current with delta-connected load; *b* – phase voltage with load star-connected, currents flowing through T_3, D_3, T_4, D_4 and the line current; *c* – phase currents and current in the supply source circuit.

2. If $0.525 \geqslant \cos\varphi \geqslant 0$, the current of phase B is only partially compensated by that of phase C and the current of the supply source changes sign. This implies that the d.c. source must take the non-compensated part of the reactive power given off by phase B. In the opposite case (supply from a rectifier), the d.c. source has to be shunted by a capacitance given by the formula:

$$C = \frac{\tau 10^6}{k r_k}\left[2\ln\frac{\left(1+e^{-\frac{1}{6f\tau}}\right)^2}{2\left(1+e^{-\frac{1}{2f\tau}}\right)} + \frac{\left(1-2e^{-\frac{1}{6f\tau}}\right)\left(1-e^{-\frac{1}{3f\tau}}\right)}{1+e^{-\frac{1}{2f\tau}}}\right][\mu\ F]. \qquad (21)$$

where $k = \frac{U_c}{U_m}$ is the permissible increase in the capacitor voltage.

The mean value of the phase current during a halfwave is found from the expression:

$$I_{\mathrm{av.p}\Delta} = \frac{2}{3}\cdot\frac{U_m}{r_k} - \frac{4U_m}{r_k} f\tau \ln\frac{1+e^{-\frac{1}{6f\tau}}}{1+e^{-\frac{1}{2f\tau}}}. \qquad (22)$$

The effective value of the phase current is:

$$I_{\mathrm{eff.p}\Delta} = \frac{U_m}{r_k}\cdot\frac{\sqrt{2}}{\sqrt{3}}\sqrt{1-3f\tau\frac{1-e^{-\frac{1}{3f\tau}}}{1-e^{-\frac{1}{6f\tau}}+e^{-\frac{1}{3f\tau}}}}. \qquad (23)$$

The relationship between the phase and line currents is the same as for a sinusoidal voltage:

$$I_{\mathrm{eff.}l} = \sqrt{3}\, I_{\mathrm{eff.p}}. \qquad (24)$$

Fig. 7(a) shows curves for the currents in phases A and B and the line current $i_l = i_A + i_B$ for delta-connected motor windings. Fig. 7(c) shows curves for the currents in phases A, B and C. In each phase the current i_1 (15) flows through the supply source, but the current i_2 (16) does not. Therefore the current in the circuit of the supply source is the sum of the currents i_A, i_B and i_C without the shaded sections:

$$I' = \frac{2U_m}{r_k}\left(1-3f\tau\frac{1-e^{-\frac{1}{3f\tau}}}{1-e^{-\frac{1}{6f\tau}}+e^{-\frac{1}{3f\tau}}}\right). \qquad (25)$$

With star-connected motor windings, the phase voltage curve is in two steps (see Fig.7b):

for the first step

$$U_{mp} = \frac{U_{ml}}{Z_A + \frac{Z_A}{2}} \cdot \frac{Z_A}{2} = \frac{U_{ml}}{3}; \tag{26}$$

for the second

$$U_{mp} = \frac{U_{ml}}{Z_A + \frac{Z_A}{2}} Z_A = \frac{2}{3} U_{ml}. \tag{27}$$

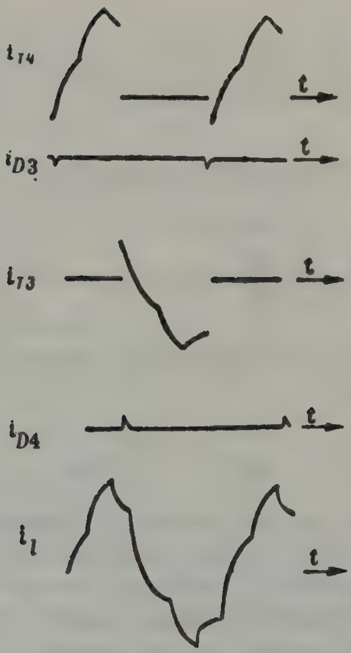


Fig. 8. Oscillograms of the triode, diode and line currents

The phase voltage does not contain harmonic multiples of 3:

$$U_p = \frac{3}{\pi} U_{mp} \left[\sum_{n=0}^{n=\infty} \frac{\sin(6n+1)\omega t}{6n+1} + \sum_{n=1}^{n=\infty} \frac{\sin(6n-1)\omega t}{6n-1} \right]. \tag{28}$$

The expressions for the currents in the individual intervals are:

if $0 \leqslant t \leqslant t_1$

$$i_1 = \frac{U_{mp}}{2r_k} - \frac{U_{mp}}{2r_k} \cdot \frac{2 + e^{-\frac{1}{6f\tau}} - e^{-\frac{1}{3f\tau}}}{1 + e^{-\frac{1}{2f\tau}}} e^{-\frac{t}{\tau}};$$

if $t_1 \leqslant t \leqslant t_2$

$$i_2 = \frac{U_{mp}}{r_k} - \frac{U_{mp}}{2r_k} \cdot \frac{1 + 2e^{-\frac{1}{6f\tau}} + e^{-\frac{1}{3f\tau}}}{1 + e^{-\frac{1}{2f\tau}}} \cdot e^{-\frac{t}{\tau}};$$

if $t_2 \leqslant t \leqslant t_3$

$$i_3 = \frac{U_{mp}}{2r_k} + \frac{U_{mp}}{2r_k} \cdot \frac{1 - e^{-\frac{1}{6f\tau}} - 2e^{-\frac{1}{3f\tau}}}{1 + e^{-\frac{1}{2f\tau}}} e^{-\frac{t}{\tau}}.$$

The curves for the currents flowing through T_3, T_4, D_3 and D_4 are given in Fig. 7(b) along with those for the line current $i_l = i_A + i_B$ with the motor connected in star.

The mean value of the phase current is

$$I_{\mathrm{eff}} = \frac{2U_{mp}}{3r_k} - \frac{2U_{mp}}{r_k} f\tau \ln \frac{\left(1 + e^{-\frac{1}{6f\tau}}\right)\left(2 - e^{-\frac{1}{6f\tau}}\right)}{1 + e^{-\frac{1}{2f\tau}}}, \tag{29}$$

and its effective value

$$I_{\mathrm{ef}} = \frac{U_{mp}}{\sqrt{2} \cdot r_k} \sqrt{1 - 3f\tau \frac{1 - e^{-\frac{1}{3f\tau}}}{1 - e^{-\frac{1}{6f\tau}} + e^{-\frac{1}{3f\tau}}}}. \tag{30}$$

The current in the supply source circuit is:

$$I'_Y = \frac{3}{2}\cdot\frac{U_{mp}}{r_k}\left(1 - 3f\tau\,\frac{1-e^{-\frac{1}{3f\tau}}}{1-e^{-\frac{1}{6f\tau}}+e^{-\frac{1}{3f\tau}}}\right). \tag{31}$$

Fig. 8 shows oscillograms of the currents i_{T4}, i_{D4}, i_{T3}, i_{D3} and i_A.

Conclusions

1. The supply of an asynchronous motor with a voltage which is of rectangular shape and contains all odd harmonics is inefficient owing to the considerable deterioration of its operating characteristics.

2. This deterioration is limited to 1-2 per cent if the supply voltage is free of harmonic multiples of 3.

3. There is no justification in using semiconductor converters for approximately sinusoidal output voltage curves. The slight improvement in the operating characteristics of the motor is offset by the complication of the system and the increased losses due to the use of an output transformer.

4. If the line output voltage of the converter has the shape of the curve in Fig. 1(a), it is advisable to connect the motor windings in star. but if the line output voltage contains no harmonic multiples of 3, star or delta connexion is equally good. If phase distortion is present, it is advisable to connect the motor windings in star in order to eliminate currents of harmonics which are multiplies of 3.

Translated by O.M. Blunn

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THE CURRENT DISTRIBUTION IN THE BATHS OF ORE-SMELTING FURNACES* **

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(Received 23 February 1961)

Modern electric ore-smelting furnaces take currents of up to hundreds of thousands of amps at voltages seldom above 200 to 300 V.

Another feature of these furnaces is the low current density of the electrodes which varies between 2 and 10 A/cm^2. Yet in the bath itself, depending on the physical and chemical state of the materials, the current density and the volume power density vary between very wide limits, with the result that large variations occur in bath temperature. In open furnaces the temperature hardly reaches 100-200°C under the charge hole, whilst in the space under the electrodes it can reach several thousand deg. centigrade.

The conversion of electrical energy into thermal energy in the bath is bound up with these factors. Depending on the physical and chemical state of the charge materials, the current distribution and the temperature, the furnace may operate like a resistance furnace, an arc furnace or like a combination of the two, when energy is liberated in the charge and arc at the same time.

The interplay of such diverse factors complicates the selection of optimum conditions. But since the starting point is necessarily the distribution of the current and power in the bath, a study of these factors is of decisive importance.

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The general laws of electrical circuits and the method of equivalent circuits can be applied to furnace circuits, but to do so it is necessary to take the spatial current distribution into account. But the generally accepted equivalent circuits of polyphase systems in the form of stars of polygons are not at all suitable for the current distribution and to a certain extent they are unrealistic.

The structure of the bath and charging the materials

The structure of the furnace bath under steady state operating conditions can be most diverse, but certain regularities are common to them all, the main ones being the temperature distribution, the current field and the aggregate state of the charge.

There is usually a zone of maximum current density on the electrode face. It is here that the main chemical processes take place most intensively in the formation of the melt. This region is called the crucible of the bath. The temperature and current density decrease from the electrode face to the perimeter of the bath.

Depending on their physical and chemical properties, the charge materials in the space under the electrodes may be in the solid, molten or gaseous states. In the first two cases the conversion of electrical energy into heat takes place as in a resistance furnace, and in the third case (the gaseous state) it takes place via the arc. Molten products of reaction accumulate in the sub-electrode space. It is to be supposed that the temperature levels and extent of the regions with various forms of the materials differ in different furnaces and with different melts. The distribution of the regions in which the materials are in a different form is also affected by the relative and absolute dimensions of the electrode and bath.

In polyphase furnaces in general and three phase furnaces in particular the crucibles of the individual phases can either be kept apart or else merged into one common crucible. The design of medium power three-phase carbide furnaces is a typical example of "isolated" crucibles. Common crucibles are used in medium power ferrosilicon furnaces.

The forogoing classification is rather rough, but unfortunately there is still a lack of reliable data about the crucibles of furnaces.

The possible paths of the current in the bath

Electric ore-smelting furnaces generally operate under mixed conditions where there is not only an arc current but also currents which shunt the arc. These currents flow through the molten and fluid materials in the bath.

In a single phase furnace the hearth usually acts as a second electrode and the arc burns between the face of the moving electrode and the materials on the hearth. Since there are materials around the arc which possess a certain electrical conductance, it follows that current flows parallel to the arc between the moving electrode and the hearth.

If the furnace is operating without the arc, a flow of current also takes place from the side surface of the moving electrode into the fluid melt or the solid charge surrounding the electrode as well as from its face. When the arc emerges, part of the face surface of the electrode is occupied by a gaseous interlayer. Under certain conditions this interlayer can occupy the entire surface of the electrode face and spread to the side surface of the electrode.

The distribution of the current in three-phase furnaces is more complicated. Besides the arc currents flowing from the electrodes to the hearth on the star system, currents can also flow from the electrodes to the hearth through the charge on the star system and from electrode to electrode on the delta system. The possible current paths in a three phase furnace are illustrated in Fig. 1.

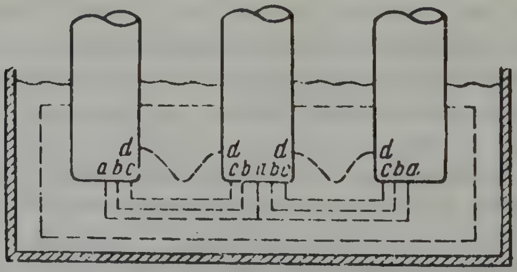

Fig. 1. Current paths in bath.
a – through arc and melt; *b* – arc apart from melt;
c – charge; *d* – electrode side and charge.

The magnitudes of the arc currents and charge conductance currents depend on many factors, the main ones being the distribution of the thermal and electrical fields in the bath and the thermal conductivity and electrical conductance of the charge materials. Since at a given voltage the configuration of the fields depends on their size, and since the electrical conductance and thermal conductivity are to a certain extent inter-connected, it follows that the density distribution of the current and power can be represented as a function of the geometric factor and the electrical conductance:

$$\delta = f(G;\ \rho).$$

Thus for both single and three-phase furnaces the problem reduces to the determination of the electrical and thermal fields, the density of the current and power in the charge, and the relationship between the power of the arc and in the charge.

The electrical field

Analytical methods of determining the electrical fields of a bath, with the non-homogeneity of the charge conductance included, are very complicated and do not always produce satisfactory results. But if it is assumed that ρ is a constant, the field of a single phase furnace can be reduced to a plane-meriodional field described by the Laplace equation

$$\nabla^2 U = 0.$$

Since the diameter of a furnace is quite large, it is possible to ignore the radial curvature of the field, and the field of a single-phase furnace can be reduced to a plane-parallel (two-dimensional) field.

Solving this equation for the vector of field intensity, we get the equation:

$$E = E_x + jE_y = \frac{U}{\delta} \cdot \frac{c}{c^2+d^2} - j\frac{U}{\delta} \cdot \frac{d}{c^2+d^2},$$

where U is the potential difference, δ the distance between the electrodes, c and d coordinates of the transformation.

It is tedious work calculating the individual points of a field, but it leads to satisfactory results. Fields obtained in this way do however have to be regarded as approximate, since no account is taken of distortions caused by the furnace lining.

In fact, a charge is present in the furnace between the electrodes having a certain electrical conductance which is limited by the insulating layer of the bath lining. The field must be regarded therefore as a two-layer field with the surface effect included.

The field configuration and current distribution depend mainly on gap δ between the electrodes, the gap a between the top electrode and the lining of the bath, the height h of the charge layer, the diameter d of the electrode and the diameter d' of the crucible.

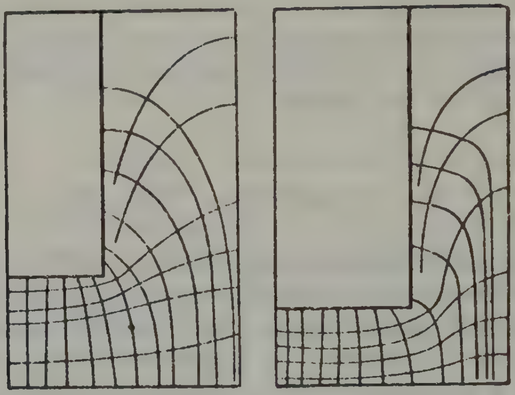

Fig. 2. Electrical field of model.

Fig. 2 shows two field configurations obtained experimentally from a furnace model. The experiments showed that the current which flows from the face of the electrode is 100 to 150 per cent of that flowing from the side surface of the electrode. But the current does not flow uniformly even from the side surface. The density of the current from the side surface at the face is greater than that on the surface of the charge by a factor of approximately 2 to 2.5. One of the curves which illustrate this relationship is shown in Fig. 3.

The charge conductance current falls sharply with increasing dimensions of the gaseous sphere surrounding the electrode face. The relationship between the current and δ is just the same. The magnitude of the charge current is least affected by the diameter of the electrode. Even over a fairly wide range of electrode diameters, the current from the side surface of the electrode only varies by 5 to 10 per cent.

As is known, technological considerations enter into the question of electrode diameter and, within the limits allowed on technological grounds, the diameter of an electrode has practically no effect on the magnitude of the current.

An analytical study of the fields of three-phase furnaces is still more complicated, but the field configuration can again be obtained by modelling.

When investigating the field of three phase furnaces, it is necessary to distinguish the instantaneous and effective values of the fields. The distribution of the energy in a bath is mainly determined by the effective value of the field.

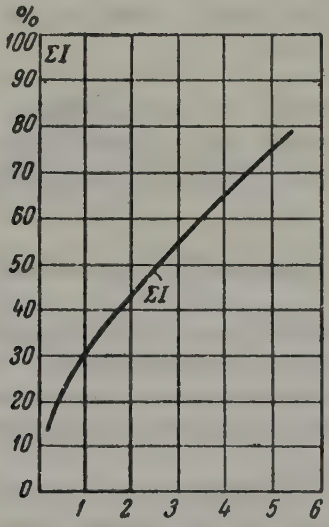

Fig. 3. Current density over height of electrode.

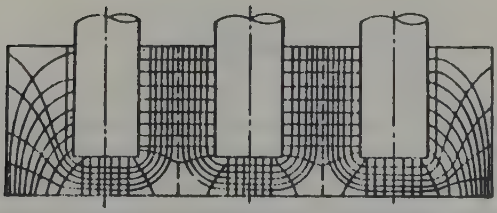

Fig. 4. Field of three-phase furnace, electrodes in row.

Fig. 4 shows the field of a furnace with its electrodes arranged in a row (a quadrangular bath was used). The configuration of the field was influenced mainly by the dimensions of the bath, the diameter *d* of the electrodes, the distance l between the axes of the electrodes, the gap δ between the electrodes and the hearth and the height h of the furnace.

Delta cross-flow of current only takes place between the middle and extreme electrodes. The current density in the vertical direction is practically constant; a more or less sharp decrease in this current occurs at the upper boundary of the bath. With uniform conductance of the medium, the delta currents are greater than the star currents (see Fig. 5). With an increase in the gap between the electrode and hearth, the delta currents decline almost linearly, but the star currents decline slightly more slowly.

However, the distribution of the currents in the bath is altered considerably by the gaseous spheres which form under the electrodes, and by the non-homogeneous electrical conductance of the bath.

The distribution of power in the bath

A uniform distribution of power is preferable for processes which take place at comparatively low temperatures. In such cases the processes take place in a considerable volume of the bath and the gaseous products rise to the throat with uniform density.

For high temperature processes it is necessary to have hearths with more intensive power capable of providing these temperatures.

In order to estimate the distribution of power in a bath, use may be made of the notion of the volume power density:

$$p=\frac{dP}{dV},$$

where P is the mean value of the power and V the volume in which the power is developed.

In differential form the volume power density can be expressed by the equation

$$p=\frac{E^2}{\rho},$$

where E is the intensity of the electric field and ρ the specific resistance of the medium at a point in the medium.

Since the volume power density is proportional to the square of the field intensity if ρ is constant, it follows that curves can be plotted for the distribution of p if the pattern of the field intensity distribution is known.

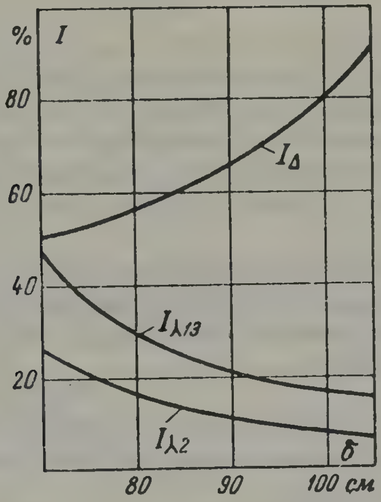

Fig. 5. Star and delta currents.

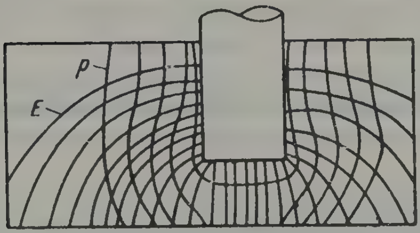

Fig. 6. Distribution of volume power density.

Fig. 6 shows the graphically-determined pattern of the distribution of the volume power density over the vertical section of a furnace bath. Lines p represent lines of equal volume power. By virtue of the square-law relationship with field intensity, the volume power density is greatest on the surface of the electrodes where E is also maximum. The value of p decreases much more rapidly than E with increasing distance from the electrode. In the majority of cases the value of p is only a fraction of one per cent of its original value at a distance from the electrode equal to the diameter of the electrode.

The electrical conductance of the charge materials

The electrical conductance of the charge materials has a fundamental effect on the current and power distribution in the bath. The main ingredients of the charge are reducing agents, oxides and metals. Their conductance will now be considered in turn.

Reducing agents. The main types of reducing agent are anthracite and coke. Practically all carbon materials have a very high specific resistance at temperatures up to 200-300°C. It declines with increasing temperature, but at temperatures between 500 and 600°C this decline is temporarily arrested. At 500-600°C to 1000-1200°C there is a marked decline in the specific resistance of carbon materials.

With a further increase in temperature, the specific resistance of all types of reducing agent balances out and the electrical conductance becomes practically the same for all types of reducing agent in the active zone of the reaction.

Fig. 7 shows typical curves for the relationship between the specific resistance of a type of anthracite and a type of coke on the one hand, and temperature on the other.

The roasting and graphitization of the reducing agent can take different lengths of time depending on the rate of descent of the charge. But in all cases the electrical conductance of almost all types of reducing agent is the same in the zone of reaction and in the lower levels of the bath generally. The type of reducing agent cannot therefore have any appreciable effect on the magnitude of the charge conductance current. Porosity and lumpiness are the only factors affecting the conductance of the reducing agent in the lower regions.

Porosity reduces the conductance of the reducing agent. The relationship between lumpiness and conductance is not so straight-forward since the number of contacts between lumps and the area of contact comes into the question. In some cases the total resistance of the bath can be changed slightly by altering the porosity and lumpiness of the reducing agent, but the scope for this is very limited.

In the upper regions of the bath, where lower temperatures prevail, all types of reducing agent have a relatively high specific resistance and therefore play a subsidiary role as a conducting factor. But the type of reducing agent does influence the magnitude of the charge conductance current. At low temperatures coke has a larger conductance than anthracite and it is therefore instrumental in increasing the conductance and charge conductance current in this region. Conversely, anthracite sharply reduces these currents, since it has a high specific resistance.

The type of reducing agent has a greater effect in the middle zone of the bath where temperatures of 500 to 1200°C prevail. In this range of temperatures there is a sharp increase in the conductance of the reducing agent and along with that, an increase in the charge conductance current and power.

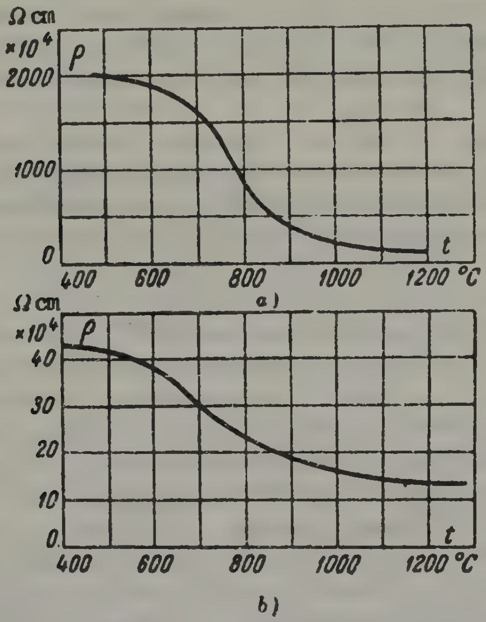

Fig. 7. Specific resistance of anthracite (a) and coke (b) as a function of temperature.

In the lower regions, where maximum temperatures prevail, the conductance of all types of reducing agent becomes maximum, and it balances out so that the conductance of this zone is maximum and all types of reducing agent have the same effect. It appears that the charge conductance currents and powers are also maximized in this zone.

As a whole it may be said that the reducing agent is not a significant factor as regards conductance in the upper regions. But in this zone types of reducing agent do affect the magnitude of the charge conductance current. In the middle regions the conductance of the reducing agent is greater and, consequently, the charge conductance currents and powers are increased. Finally, the conductance of the reducing agent is maximized in the lower regions where the temperature is greatest and all reducing agents tend to have the same effect. In this zone the charge conductance currents and powers are also maximum.

Oxides. Metal oxides have a very high specific resistance and a negative temperature coefficient, i.e. their specific resistance declines with increasing temperature. Even at very high temperatures near their melting point, most oxides still retain a high specific resistance. For example, the specific resistance of quartzite at room temperature is about (2-3) x 10^{10} Ω cm and at 800°C it is about (5-6) x 10^3 Ω cm, i.e. it is still high.

At 0°C the specific resistance of calcium oxide is about (2-3) x 10^{13} Ω cm; at 500°C, (7-8) x 10^8 Ω cm; at 1200°C, 1 x 10^3 Ω cm. It falls still lower at higher temperatures, but it is still quite high.

In the upper region of the bath the main types of oxides can therefore be regarded as non-conducting inclusions between the lumps of reducing agent. In the middle and lower regions they begin to play a certain role in the conduction of current, but their conductance is still very small.

Metals. At 20°C the specific resistance of iron is about 10 x 10^{-6} Ω cm. Its specific resistance is increased six times over with a rise in temperature from 20 to 1100°C. Nevertheless, the electrical conductance of iron is high even at quite high temperatures. Other metals have more or less the same electrical properties. The presence of metallic components in the composition of the charge has a marked effect on the transformation of electrical energy in the bath of the furnace. Since the electrical power of a furnace is proportional to its resistance, and since the resistance of metallic inclusions is negligible, the energy liberated in these inclusions is therefore negligible compared with that liberated in the other elements of the furnace.

The conductance of the charge mixture. The charge of ferrosilicon furnaces is generally a mixture of reducing agent, oxides and metals, but that of a carbide furnace is usually only a combination of reducing agent and oxides. It will already have been seen that the conductance of these components is widely different. The conductance of metals is almost a dozen times greater than that of oxides. The conductance of reducing agents is about half that of metals. The electrical conditions of the bath are affected accordingly.

In the upper regions of the furnace the current can only flow through the small circuits formed between the lumps of reducing agent and metal (iron), since the lumps of the oxide with their high specific resistance can play no part in the conduction of current. Therefore currents will flow through the charge only where such circuits are formed in series.

Since the specific resistance of metal is about half that of the reducing agent, it follows that the electrical energy of the charge conductance currents is mainly liberated in the reducing agent and not in the metal. On the whole the charge conductance current in the upper cooler zones of the furnace is not considerable.

In the middle regions of the furnace the conductance of the oxides and reducing agent increase, whilst that of the metal decreases. Therefore the oxides and metal takes an increasing part in the conduction of current and the transformation of energy. But in those regions the conductance currents remain inconsiderable since current-conducting circuits are seldom formed there either.

In the third and hottest zone the conductance of the oxides and reducing agents increases still further and the aggregate state of the charge undergoes a marked change as the oxides and metals soften and melt. This increases the contact between the individual lumps of the charge and whole current conducting regions are formed besides individual circuits. The difference between the volume power density of the various charge constituents is reduced.

The electrical conductance of the melt. A melt is a finished or intermediate product in the fluid state. It lies at the bottom and constitutes a section of the current path in the bath.

The energy which is liberated in the melt plays a greater or lesser role in the overall energy balance of the bath depending on the dimensions of the bath and the specific resistance of the melt.

In ferroalloy furnaces the melt is a fluid ferrosilicon with a 10 to 50 per cent iron content. This iron content of the melt cannot ensure the necessary resistance.

The conductance of the other component (silicon) is fairly high. Fig. 8 shows the specific resistance of silicon as a function of temperature. In the solid state it has a high specific resistance (15,800 $\mu\,\Omega$ cm at 1250°C, 2500 $\mu\,\Omega$ cm at 1670°C), but after melting its specific resistance drops to 36 $\mu\,\Omega$ cm. Therefore the specific resistance of a fluid melt in a ferrosilicon furnace is approximately 100 $\mu\,\Omega$ cm. Contact between the electrode and melt, no less than the submerging of the electrode in the melt, will lead to short-circuiting in these furnaces. Therefore the energy in ferrosilicon furnaces is mainly liberated in the arc. Even though the arc and melt are connected in series, the resistance of the melt is so small in comparison with that of the arc that it can be ignored.

The difference between this type of furnace and steel furnaces is that a ferrosilicon charge in the solid state has a much greater resistance than steel scrap in a steel furnace. Therefore contact between electrode and charge does not in this case cause a short-circuit and a finite current flows through the charge from electrode to electrode or to the melt.

The relationship between the resistances of the arc and charge on the one hand and the power liberated in them on the other still remains an open question. The temperature of a charge and its resistance vary within wide limits and therefore in certain conditions the resistance of the charge can be commensurate with that of the arc. Therefore the energy which is liberated in the charge may be commensurate with that liberated in the arc. The baths of furnaces have a certain capacity for self-regulation and re-distribution of power between arc and charge. The melt takes no part in this distribution of power.

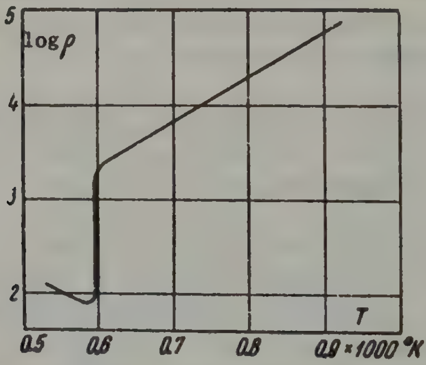

Fig. 8. Specific resistance of silicon as a function of temperature.

The specific resistance of calcium carbide also varies widely as a function of temperature. Fig. 9 shows a curve obtained by R. Chuguryan, from which it will be seen that a marked reduction of specific resistance only sets in at temperatures over 1600-1700 °C. Since the temperature of the melt in the fluid (or viscous) form varies between 1800 and 2100 °C, the specific resistance of a calcium carbide melt may be taken as 0.08-0.25 Ω cm. Certain other authors have produced higher figures (0.2-0.3 Ω cm). But even if the average specific resistance of of molten carbide is taken as 0.1 Ω cm, the total resistance of the bath is quite considerable.

For example, in particular 7500 kVA furnace the effective resistanc of the whole bath is 2000 $\mu\Omega$ and that of the melt 200 $\mu\Omega$ This implies that about 10 per cent of the energy is liberated in the melt. Since a considerable part of the energy is liberated in the charge which shunts the electrodes, it follows that the proportion of energy which is liberated in the arc of a carbide furnace is much less than that of a ferrosilicon furnace. The re-distribution of power may however be adjusted within certain limits by changing the operating conditions of the furnace.

It should however be mentioned that the specific resistance of calcium carbide falls very quickly with a decrease in temperature. The conducting layers of the charge and melt are therefore confined to layers surrounding the electrodes. The layer of the melt directly adjacent to the hearth of the furnace has such a high specific resistance that the current practically cannot penetrate it at all, and the hearth electrodes are equi-potential. The layers of the charge on the surface near the throat also have a very high specific resistance and therefore the density of the charge conductance current is practically zero in this region.

The actual distribution of the current and power in the bath

It has been argued above that the current and power distribution depends on the geometric dimensions of the bath and the electrical conductance. A change therefore in the conductance of the charge with variations in temperature causes considerable distortion of the current distribution compared with that which would otherwise exist in a homogeneous medium. Since the electrical conductance of ferrosilicon differs considerably from that of carbide, its effect on the distribution of current will not be the same in both cases. These types of furnace will now be considered along with steel and glass furnaces.

In steel furnaces the charge and melt both display high electrical conductance. Therefore at permissible values of current the liberation of heat in the charge and melt is negligibly small. Only arc furnaces can be used here. The electrical energy of the arc is transformed into thermal energy and transferred to the charge and melt according to laws of heat transfer.

In ferrosilicon furnaces the charge mixture has a rather high specific resistance. Charge conductance currents do occur in these furnaces. In the upper regions of the bath the specific resistance is much greater than in the middle regions, and in the region adjacent to the electrode face the conductance is still higher. The charge conductance currents in ferrosilicon furnaces flow mainly in the narrow zone surrounding the face of the electrode. The melts of the great majority of ferrous alloys have very low electrical conductance and therefore the volume power density of the melt is inconsiderable. The melt takes no significant part in the transformation of electrical energy into thermal energy in these furnaces. In slagless processes the electrode face is not usually in contact with the melt and the arc burns on the electrode. If slag is present, the electrode face is usually submerged in the slag and the arc burns on the molten slag or on the melt. Even if slag is present, electrical energy is converted into thermal energy in only 20 to 30 per cent of the volume of the bath. Outside this space the volume power density is very low. The relationships between the power of the arc and charge can vary widely depending on the geometry of the bath and the type of alloy. For example, arc power may reach 80 to 90 per cent for ferrosilicon with a large iron context, but it can be as little as 30 to 40 per cent for carbon ferromanganese.

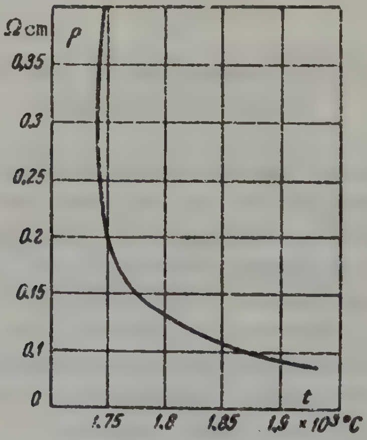

Fig. 9. Specific resistance of calcium carbide as a function of temperature.

In carbide furnaces the charge mixture has a high specific resistance in the cold state. But with increasing temperature it rapidly declines. Therefore the charge conductance current is small in the upper part of the furnace and the region in which electrical energy is converted into thermal energy occupies no more than 30 to 40 per cent (by volume) of the bath.

The specific resistance of calcium carbide melts is much greater than that of ferroalloy melts. In the carbide furnace, the face of the electrode is submerged in the melt, or else the arc burns on it; under normal conditions the presence of an arc is desirable since it ensures the necessary high levels of temperature for the technological process. A number of investigations have shown that about 40 per cent of the energy is liberated in the arc in carbide furnaces; roughly the same amount of energy is liberated in the charge and the remaining 20 per cent is liberated in the melt.

A glass furnace is a good example of a furnace with a melt having a high specific resistance. It operates on the principle of a resistance furnace. No electric arc is used in these furnaces.

Translated by O.M. Blunn

AN INVESTIGATION OF THE STEADY STATE STABILITY LIMITS OF LONG DISTANCE TRANSMISSION LINES WITH CONTROLLED SYNCHRONOUS CONDENSERS*

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(Received 13 April 1961)

The installation of controlled synchronous condensers at intermediate substations is one of the most effective means of increasing the carrying capacity of long distance transmission lines.

An investigation of the steady state stability of long distance transmission lines with several synchronous condensers provided with automatic high-response excitation regulators is a very complex problem since it requires the formation and analysis of a high order characteristic equation. This equation is of order 12 for two synchronous condensers and of order 20 for four [1]. Theoretical investigation of steady state stability limits has until now been confined to systems with one synchronous condenser.

In this paper a method is proposed for long distance transmission lines with several controlled synchronous condensers. This method, evolved in the problem laboratory for electrical systems at the Moscow Power Institute, permits analysis of steady state stability as if only one controlled synchronous condenser were present. The changeover is based on comparison of regions of stability in the co-ordinates of the amplification coefficients of the automatic excitation regulator, for electrical transmission with one or two intermediate substations. Calculations carried out by the authors have shown that the stability limits of each transmission system practically coincide in modes of operation corresponding to equal maximum impedances Δx of the branch of the condenser beyond which the e.m.f. is maintained constant in order to ensure steady state stability.

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* *Elektrichestvo,* 9, 66-69, 1961.

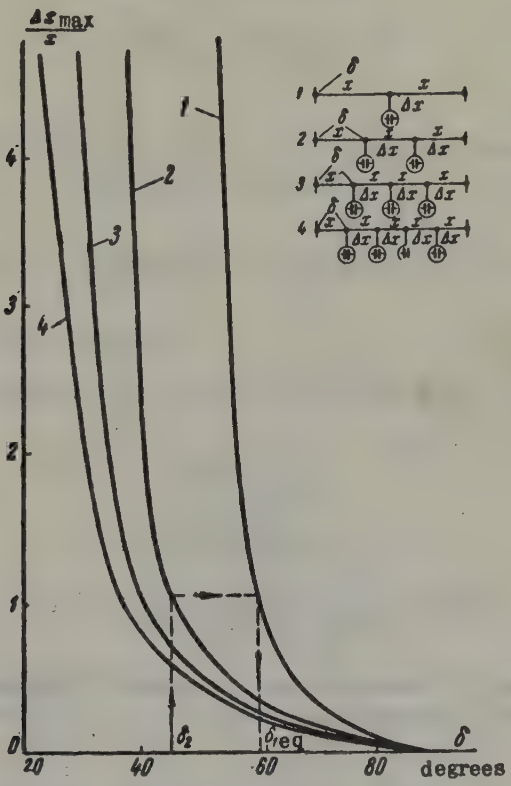

Fig. 1.

Fig. 1 shows transmission systems corresponding to the normal operations of a single-circuit or twin-circuit line with one, two, three and four intermediate substations.

It is assumed that the line sections are all equal in length, that the voltages at the sending and receiving ends of the line are constant and equal to unity, and that the voltage level at the point where the synchronous condenser is connected to the line is also equal to one. The condenser branch is replaced by an impedance Δx and a constant e.m.f. E_x after it. Ignoring the capacitance of the line, the following equations can then be written for the transient phenomena in system 1 for small deviations in conditions:

$$T_{J1}p^2\Delta\delta_1 = -\Delta P_1; \tag{1}$$

$$T_{J2}p^2\Delta\delta_2 = -\Delta P_2; \tag{2}$$

$$T_{J3}p\Delta\delta_3 = -\Delta P_3 \tag{3}$$

or

$$p^2\Delta\delta_{13} = -\frac{\Delta P_1}{T_{J1}} + \frac{\Delta P_3}{T_{J3}}; \tag{4}$$

$$p^2\Delta\delta_{32} = -\frac{\Delta P_3}{T_{J3}} + \frac{\Delta P_2}{T_{J2}}, \tag{5}$$

where

$$\Delta P_1 = \frac{1}{x_{12}}\cos\delta_{12}(\Delta\delta_{13} + \Delta\delta_{32}) + \frac{E_x}{x_{13}}\cos\delta_{13}\Delta\delta_{13}; \tag{6}$$

$$\Delta P_2 = -\frac{1}{x_{12}}\cos\delta_{12}(\Delta\delta_{13} + \Delta\delta_{32}) - \\ -\frac{E_x}{x_{23}}\cos\delta_{32}\,\Delta\delta_{32}; \tag{7}$$

$$\Delta P_3 = -\frac{1}{x_{13}}\cos\delta_{23}\Delta\delta_{13} + \frac{E_x}{x_{23}}\cos\delta_{32}\Delta\delta_{32}. \tag{8}$$

The condition for balanced reactive powers at an intermediate point may be expressed in the following way:

$$2Q_l + Q_{s.c} = 2\left(-\frac{1}{x} + \frac{1}{x}\cos\delta\right) + \left(-\frac{1}{\Delta x} + \frac{E_x}{\Delta x}\right) = 0.$$

Using this expression, the e.m.f. can be defined as a function the angle δ:

$$E_x = 1 + 2\frac{\Delta x}{x}(1 - \cos\delta). \tag{9}$$

Substituting expressions (6)-(9) in the transient behaviour equations (3) and (4), and taking into account in forming the characteristic equation that

$$x_{12}=2x+\frac{x^2}{\Delta x};$$

$$x_{13}=x_{32}=x+2\Delta x;$$

$$\delta_{13}=\delta_{32}=\delta;$$

$$\delta_{12}=\delta_{13}+\delta_{32}=2\delta,$$

the following expression can be formed for the free term of the characteristic equation:

$$a_0=A\left[2\frac{\Delta x}{x}\cos^2\delta-2\frac{\Delta x}{x}(1-\cos\delta)+\cos\delta\right], \qquad (10)$$

where A is a constant coefficient

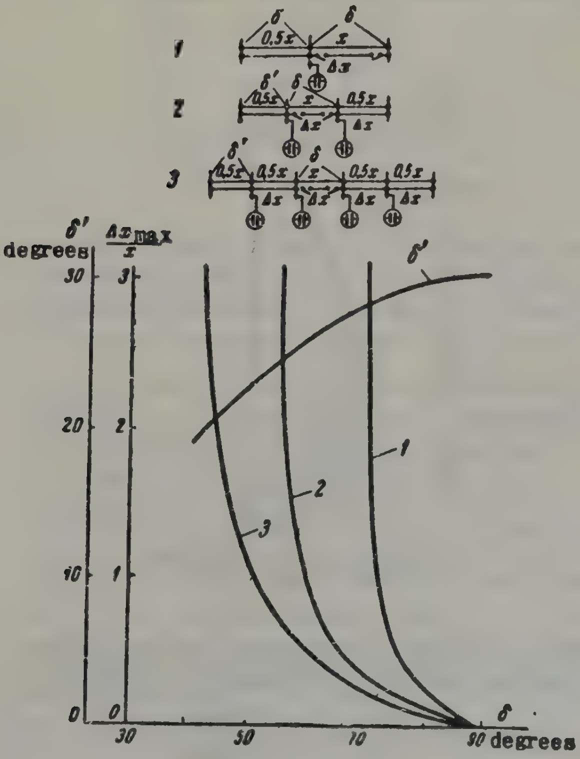

Fig. 2

Discarding the coefficient A and equating the second multiplier on the right-hand side of (10) to zero, that value of $\Delta x/x$ is defined as a function of the angle δ which is maximum as regards the condition of maximum stability and beyond which the e.m.f. E_x is held constant in order to ensure steady state stability:

$$\frac{\Delta x_{\max}}{x} = \frac{\cos\delta}{2 - 2\cos\delta - 2\cos^2\delta}. \tag{11}$$

The relationship $\Delta x_{\max}/x = f(\delta)$ is represented by curve 1 in Fig. 1. In this diagram the curves 2, 3 and 4 refer to an analogous relationship in respect of two, three and four synchronous condensers. Fig. 2 shows the same relationships again, but this time for post fault conditions in a two-circuit transmission line.

In order to plot the region of stability, resort is had to characteristic equations [2] which are formed on the assumptions usually made in the analysis of the steady state stability of electrical systems with automatic high-response excitation control.

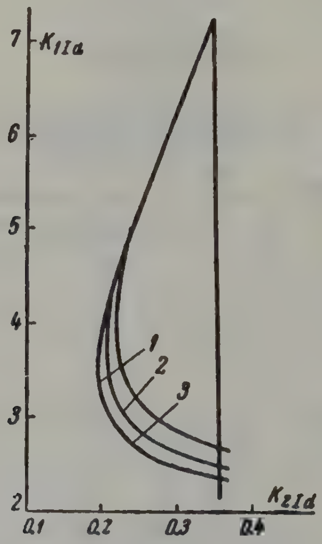

Fig. 3. Effect of the inertia constant of the receiving system on the region of stability:

$$K_{01d} = 2.5;\ \delta = 60°;\ K_{TJ} = \frac{T_{\text{syst.}}}{T_{\text{IS.C}}}.$$

1) $K_T = 1$; 2) $K_T = 3$;
3) $K_T = \infty$.

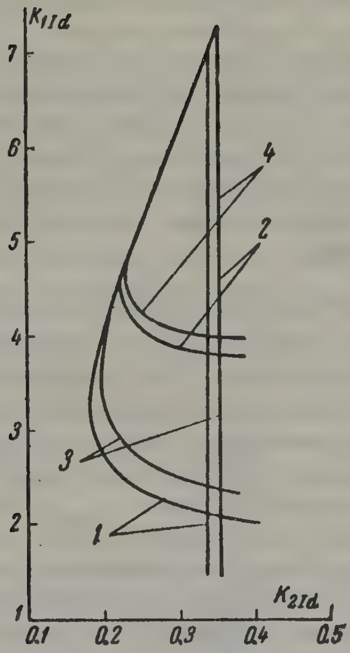

Fig. 4. Regions of stability in regulation of the excitation according to the deviation of the first and second derivatives of the direct axis current of the synchronous condenser ($K_{0Id} = 25$):

1 – for a system with two synchronous condensers at $\delta = 45^0$; 2 – the same at $\delta = 60^0$; for the equivalent system with one synchronous condenser ($\delta_{eq} = 60^0$); 4 – the same with δ_{eq} 70^0.

Line parameters: length of section 500 km; $x_0 = 0.223\ \Omega/\text{km}$; $b_0 = 4.94 \times 10^{-6}\ \frac{1}{\Omega}$ /km; nominal line voltage 650 kV

Parameters of synchronous condensers: $w_{s.c} = 1500$ MVA; $x_d = 2.384$; $x_q = 1.284$; $x_d' = 0.36$; $x_T = 0.12$; $T_{d0} = 10.2$ sec; $T_p = 0.05$ sec; $T_{Js.c} = 4.6$ sec.

Parameters of sending station: $P = 3000$ MW; $T_{Jst} = 12.4$ sec.

It is also assumed that the inertia constants of the sending and receiving stations are equal so that all the systems except 1 in Fig. 2 become symmetrical, which permits factorization of the characteristic equation and simplifies the plotting of the stable regions considerably.

Fig. 3 illustrates the effect of the inertia constant of a receiving station on the region of stability for a transmission line with one synchronous condenser. It will be seen from this diagram that the stable region is hardly altered by an increase in the inertia constant of the receiving station from the value of the inertia constant of the sending station to infinity. Hence the extra assumption made above involves no great error.

Figure 4 shows the limits of stability with excitation control of synchronous condensers with respect to the first and second derivatives of natural direct axis current. The limits are plotted for two modes of transmission using two synchronous condensers (i.e. angles $\delta = 45^0$ and $\delta = 60^0$) and for transmission with one synchronous condenser with the corresponding angles ($\delta_{eq} \approx 60^0$ and $\delta_{eq} \approx 70^0$) found from curves 1 and 2 in Fig. 1 on condition that $\Delta x_{max}/x$ is the same. Fig. 5 shows the regions of stability for systems 1 and 2 in Fig. 2, i.e. for the post fault conditions of coupled transmission with one and two synchronous condensers. In plotting the regions it was assumed that the impedance of all sections of the line was the same and that all the synchronous condensers had identical power and other parameters.

The good agreement between the regions of stability in Figs. 4 and 5 and the results of other calculations by the authors justify the conclusion that, as regards steady state stability limits, electrical transmission is similar with either one or two synchronous condensers provided.

$$\frac{\Delta x_{max}}{x} = \text{idem}, \qquad (12)$$

and if the impedance of the line sections are the same and power and parameters of the synchronous condensers are the same.

The condition for identical line section impedances and identical condenser powers and parameters can be written in the form:

$$\frac{x_{d\,s.c}}{x} = \text{idem}. \qquad (13)$$

The conclusion regarding the similarity of transmission with two synchronous condensers and that with one condenser can also be extended to transmission with a larger number of intermediate substations, i.e. it can be said that, as regards the steady state stability limit, transmission with n synchronous condensers, with the transmitted power corresponding to the angle δ between the voltages at the ends of each section, is similar to transmission with one synchronous condenser, with the transmitted power corresponding to the equivalent angle δ, provided condition (13) is fulfilled. In other words, transmission with n synchronous condensers in a mode corresponding to the angle δ can be replaced, as far as the steady state stability limit is concerned, by transmission with one synchronous condenser having the same length of section and identical synchronous condenser power and which operates in a mode corresponding to the equivalent angle δ_{eq}.

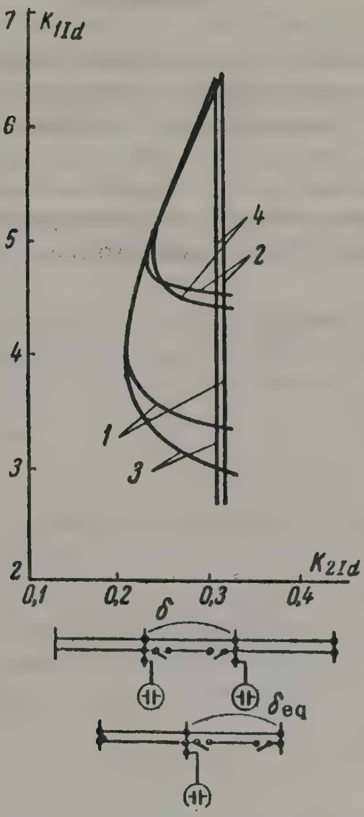

Fig. 5. The same as Fig. 4, but for two-circuit coupled transmission with interruption of one circuit on one section: 1, 2 – from the complete characteristic equation; 3, 4 – from the proposed simple method.

The equivalent angle for a transmission line with one synchronous condenser can be found from the curves in Figs. 1 and 2, provided condition (12) is satisfied, and from the known angle δ of transmission with n synchronous condensers. Further investigation of stability entails plotting the regions of stability for transmission with one synchronous condenser in coordinates of the amplification coefficients according to the derivatives of the parameter used to regulate the excitation. To plot the regions of stability, use may be made of the characteristic equation proposed by one of the authors of this article in a previous paper [2].

Translated by O.M. Blunn

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ABSTRACTS FROM PAPERS PUBLISHED IN ELEKTRICHESTVO No. 9, 1961

Control engineering

Capacitor stabilization in amplidyne-type motor generator sets. D.A. Kaminskaia *et al.*, (pp. 78-82).

It is considered that the use of stabilizing transformers in amplidyne-type motor generator sets unduly complicates the system and it is proposed to use capacitor stabilizing devices instead. Tests on a large excavator have demonstrated the advantages of the new system.

The accurate performance of automatic control systems under steady state conditions. V.T. Bardachevskii *et al.*, (pp. 22-25).

A general formula is proposed for pre-determining the steady state error in the performance of automatic control systems under the effect of several different perturbing factors.

Costing

Certain aspects of costing in power engineering. A.V. Voronin, (pp. 63-66).

It is proposed to extend the existing soviet system of percentages to operating costs (including appropriations for capital repairs and renovation) and to dispense with the system of two-variant comparison for investment purposes, using estimates of annual outlay instead.

Induction pumps

The optimum dimensions of induction pumps for liquid metals. N.M. Okhremenko, (pp. 10-16).

A detailed mathematical study is made of the optimum geometric relationships in the design of flat, spiral and cylindrical induction pumps from the point of view of maximum efficiency and minimum weight of active materials.

Insulation

Pre-breakdown phenomena in the insulation of high voltage electrical machines. B.D. Vakser *et al.*, (pp. 70-73).

An account is given of investigations into specific discharges which have been observed in mica plates prior to final breakdown. A study is made of the factors affecting the amplitude, duration and repetition rate of these discharges. The authors advocate using details of such partial breakdowns for control over insulation quality.

Power systems

Studying long line transients with phase trajectories of a non-linear load. R.I. Karaev, (pp. 26-29).

With a view to studying internal surges in long distance transmission, the author proposes a graphical-analytical method of studying transient phenomena in long lines, with non-linear loads included (transformers, chokes, arresters and so on). A differential equation is formed linking flux and current. After various transformations a family of phase trajectories is plotted in which each curve corresponds to a fixed value of a certain parameter, p.

Surge interference with carrier channels along transmission lines. G.V. Mikutskii, (pp. 51-57).

An account is given of the research which has been undertaken at the All-Union Electrical Energy Research Institute into the effect of atmospheric overvoltages, short-circuits and switching on carrier wires (communication channels) along high voltage transmission lines.

A method of determining the economic efficiency of hydro-electric stations. D.S. Stepanov, (pp. 74-78).

The official method of estimating the cost of hydro-electric stations is severely criticized.

Operating experience with telemechanical systems in the Moscow Power system. N.S. Shabalin (pp. 82-86).

A frank account is given of the problems and the type of remote control equipment in use in the Moscow Power System covering the despatcher control system, the high voltage network and the urban cable distribution system.

Relays and protection

Use of the Hall effect for a power directional relay.

V.G. Doroguntsev, *et al.*, (pp. 57-63).

A study is made of the sensitivity of power directional relays based on the Hall effect. It is proposed to improve their sensitivity by reducing the variation in induction, the use of a.c. amplifiers and an instantaneous increase in the input power of the pickup, (Hall transducer).

Rotating machines

The effect of dissymmetry of the main pole m.m.f. on the compoles and machine characteristics. I.Z. Ageyev, (pp. 37-40).

It is shown that asymmetry of the m.m.f. of the main poles of 6 kW electric motors with $2p = 6/3$ adversely affects the commutation and operating characteristics of the machines, especially in machines with half the number of compoles and with the series and parallel windings on poles of opposite polarity. However, it is proposed to turn this effect to good advantage in generators with a wide range of speeds and a half number of compoles by placing parallel coils with 25 to 35 per cent more turns on poles of opposite polarity to the compoles, than those on poles of the same polarity as the compoles.

Economic losses and the dimensions of electrical machines and transformers, I.M. Postnikov, (pp. 2-9).

An attempt is made to establish the general principles to be followed in estimating electrical machines and transformers to achieve minimum factory and operating costs.

The maximum torque of electric motors for coal-cutters.

N.A. Kiklevich, (pp. 41-44).

Using electric motors of coal cutters by way of example, the author establishes more precise criteria for the maximum torque of motors supplied from low power networks as in coal-mines.

ABSTRACTS FROM PAPERS PUBLISHED IN ELEKTRICHESTVO No. 10, 1961

Editorial

The great programme for building communism. (pp. 1-5).

The article defines communism from the party programme and briefly considers the next twenty years of development. The role of the communist party is discussed along with the part to be played by science in forming the material-technical basis of communism.

Automatic control

Charging a capacitor from an a.c. circuit through a valve.
S.M. Smirnov, (pp. 60-64).

A study is made of the process of charging a filter capacitor by sinusoidal voltage pulses. Cases of purely capacitive and capacitive-active loads of rectifiers connected in half-wave and full-wave rectifying systems are considered.

Transforming Grey's code into a binary code. V.A. Kougiis, (p. 74).

The direct use of Grey's parallel code for control devices in servo systems (e.g. for determining the angle of rotation of shafts) is not considered convenient in programme control systems based on potential logical elements. Accordingly a system of transforming Grey's code into a parallel binary code is suggested.

Control engineering

Fundamentals for constructing a standard series of automatic motor drives for multi-cable mine hoists.
V.S. Tulin, (pp. 12-19).

A study is made of the regulations and standards governing mine hoisting and lifting equipment with a view to forming a theoretical basis for standardization and greater efficiency in electrical drives.

An instrument for measuring active power. Ys.I. Stiop, (pp. 65-66).

A description is given of a device for measuring power in which the main part, squaring the sum and difference of the input quantities (current and voltage), uses tyrite elements as the non-linear resistors with a load resistance to adjust the volt-ampere characteristic.

A controllable d.c. drive. A.I. Ioffe, (pp. 71-73).

A study is made of a new variable d.c. drive with a simple converter. The drive is based on a typical d.c. motor with independent excitation with its speed controlled by varying the voltage applied to the armature from the converter. The converter consists of a three-phase transformer with primary secondary windings connected in star and connected to the a.c. network. The secondary windings have different numbers of turns so that the e.m.f. is different in each winding. Rectified voltages can be fed to several drive motors at different speeds.

Power systems

Appraising electric power costs in designing industrial plants. D.S. Stepanov, (pp. 6-11).

The estimation of the cost of electrical energy in planning industrial undertakings is considered in the light of socialist methods of planning without resort to existing tariffs.

How the post-breakdown voltage on the arrester depends on the current wavefront length. V.I. Pruzhinins-Granovsksis *et al.*, (pp. 53-54).

An account is given of investigations into the relationship between residual voltages and the length of the current wavefront for various "tervite" and vylite" resistors in soviet lightning arresters and combined lightning and surge arresters.

Discriminative ground fault signalling. V.V. Shut', (pp. 68-71).

A description is given of a recently developed device for selective indication of earth faults in compensated three-phase high-voltage networks. The device has passed tests in the Odessa power system.

It utilizes the transient currents which flow through the elements of the system at the instant an earth fault occurs.

Relays and protection

The behaviour of overcurrent relays during out-step conditions in a power system. S.E. Stepurin, (pp. 55-60).

Investigations have confirmed that overcurrent relays may operate incorrectly during out-of-step conditions of the generators. It is proposed to overcome this by adjusting the reset time of the relay or using a voltage starting relay with a particular voltage setting. The method of analysis is also applicable to other types of voltage and current relay.

The relationship between the curie point and the composition of copper-zinc ferrites. A.I. Andrievskii *et al.*, (pp. 66-68).

An account is given of investigations into copper-zinc ferrites of various compositions corresponding to values of the Curie point between the boiling point of liquid nitrogen and 460°C. The Curie points of the compositions were determined to within 4 to 6°C by the method described. Particular compositions are used for thermal relays which are also described.

Rectifiers

Calculating the parameters and characteristics of a semiconductor rectifier with saturated reactor control.

G.P. Mostkova et al., (pp. 38-46).

The authors propose a simple method of analysis to determine the parameters of the main equipment of semiconductor rectifiers controlled by saturable reactors (mainly the parameters of the reactor) and to construct the operating characteristics of the rectifier under different operating conditions.

Rotating machines

The maximum torque of coal-cutter induction motors connected in mine networks. B.B. Traube, (pp. 19-23).

In order to ensure the necessary conditions for a highly efficient performance from coal-cutting combines, the author seeks to elucidate the maximum possibilities of mine networks by finding the general laws which govern the variation of the actual torque of induction motors. Test results appear to confirm the theoretical propositions.

Locus diagrams for synchronous generator control devices with phase compounding.

V.N. Breev *et al.*, (pp. 29-34).

The properties of the various control devices in the steady state excitation circuit of generators with phase compounding are described by a single diagram so that the effect of the parameters of individual circuit elements on the excitation current can be established and so that a preliminary selection can be made of requisite initial coefficients depending on the regulation and forcing of the excitation current.

A new way of mounting brushes in electrical machines.

V.G. Gurin, (pp. 47-48).

An account is given of tests and calculations which indicate that the most efficient position of "reactive" brushes in electrical machines is that with "reverse inclination" (trailing brushes) at angles greater than 10 or 150 to the radial direction except when vibration is excessive.

The performance of a three-phase induction motor having an unequal number of turns in the stator phases.

I.M. Kamen', (pp. 48-52).

General equations are formed for the analysis of the operating conditions of non-salient pole machines having a three phase winding with identical phase zones, but with different numbers of turns in the phases.

Traction

Contactless control of electric rolling stock.

I.B. Bashuk *et al.*, (pp. 24-29).

A description is given of the authors' general-purpose systems of contactless electrical interlocking patented in 1957 and 1960. One system is a combination of a transistor and a "pick-up" whose output voltage is fed to the input to the triode. The moving core of the pick-up is mechanically linked with the apparatus to be controlled so that in one position the output voltage of the "pick-up" is zero and in another position it is sufficient to change the conducting triode into the saturated state. The triode accordingly plays the role of a normally closed or normally open blocking contact.

Various other systems are considered with diagrams.

Synchronous machine damper winding losses due to voltage time harmonics. Yu. A. Kulik, (pp. 34-37).

A study is made of the effect of voltage harmonics on the currents of the damper winding and the resultant losses. The distribution of the currents in the damper winding is analysed and the total losses caused by higher time harmonics are determined by the mean square value of the current in the cores of the damper winding.

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